The

ENGINEERS' DIGEST

REVIEW OF WORLD ENGINEERING PROGRESS

VOL. XIII No. 2. FEBRUARY, 1952

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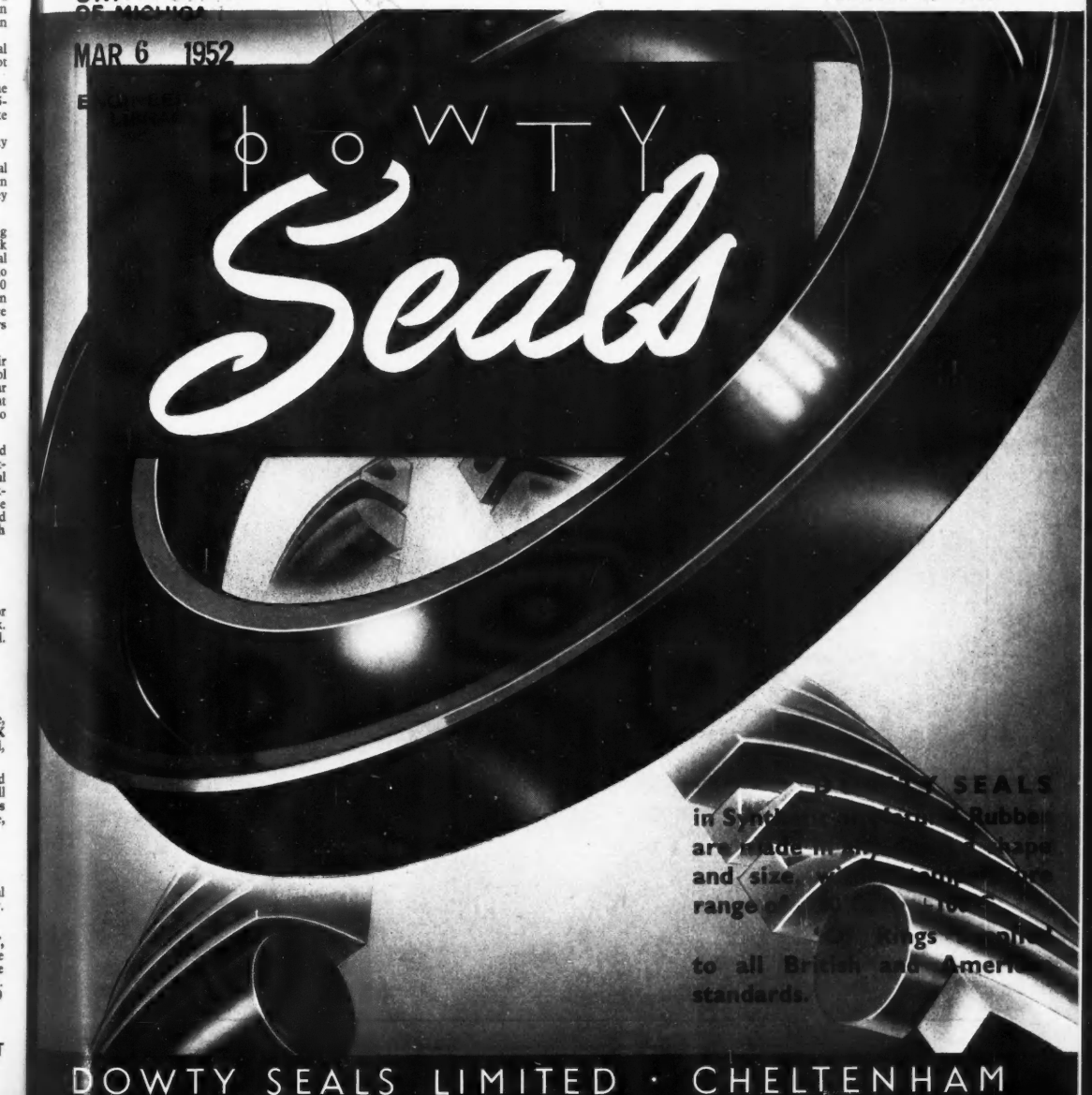

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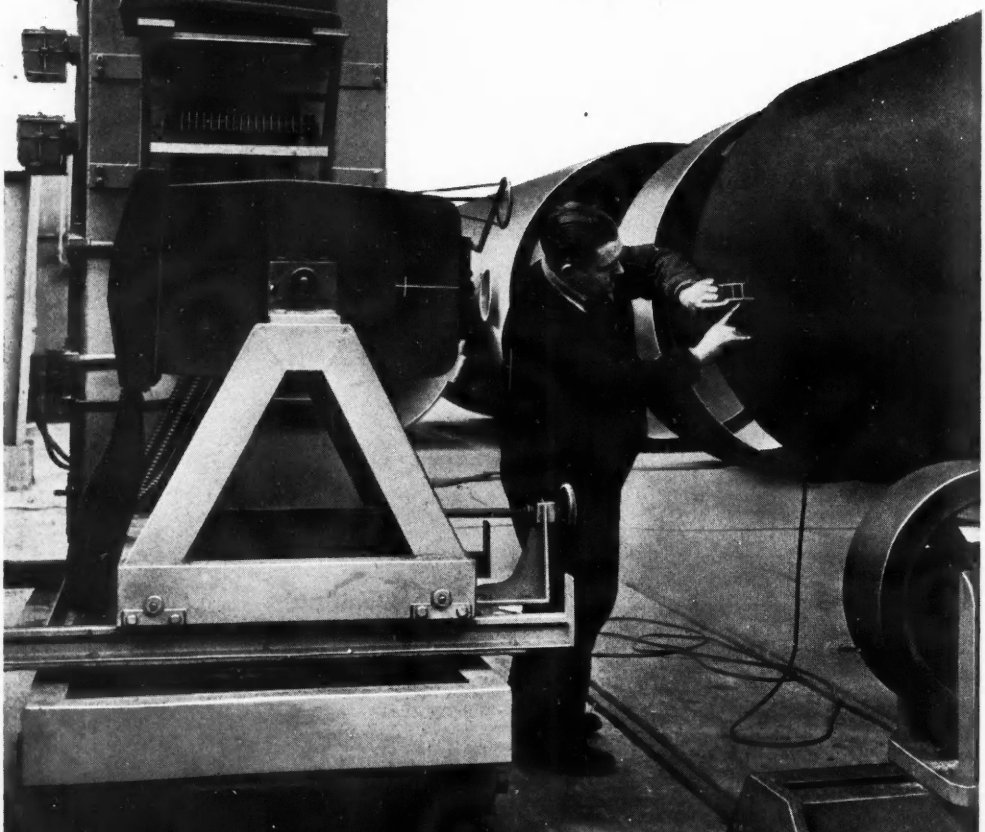

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The ENGINEERS' DIGEST

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THE BLUE PAGES

A QUICK GLANCE AT THE LATEST TECHNICAL DEVELOPMENTS THROUGHOUT THE WORLD

● *XERORADIOGRAPHY*

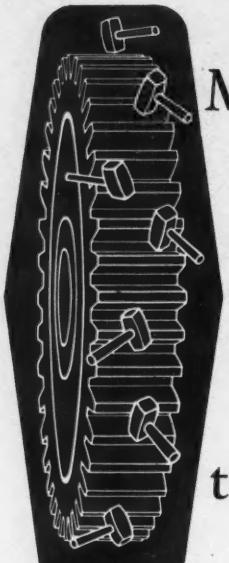
Xeroradiography is a novel form of radiography and is essentially a physical, dry, process. No wet processing or chemical solutions are required to develop the X-ray image. This new technique utilizes the phenomenon that certain materials, such as one form of selenium and sulphur, are normally good insulators, but, when exposed to X-rays, become relatively good conductors of electricity. The xeroradiographic plate takes the place of the X-ray film. It consists of an electrically conductive backing material, such as metal sheet or coated glass, the face of which is coated with a smooth layer of a suitable semi-insulator sensitive to X-rays. This coating, which may be selenium or sulphur, becomes electrically conductive when exposed to X-radiation. This plate must be electrically sensitized by passing it beneath fine wires maintained at some 7,000 V potential. The resultant corona discharge will then deposit an electrostatic charge on the plate. Wherever the coating is struck by X-rays during exposure, the selenium coating becomes conductive and discharges the electrostatic surface charge into the backing material. The X-ray image pattern, therefore, produces a latent electrical image on the plate. This invisible electrical charge distribution is made visible by causing a specially prepared dry developing powder to flow over the plate. This powder is attracted to the charged portions of the plate and adheres tenaciously, but the irradiated portions of the plate which have lost their charge are unable to retain the powder, which therefore falls off. The result is a positive X-ray image of the radiographed object. Developing may be carried out immediately after exposure and requires only a few seconds.

● *PERMANENT-MAGNET ALTERNATORS*

Permanent magnet alternators have been used for many years in small, e.g. fractional horsepower units, but until recently have not been adopted widely for power generation. With the development of dispersion-hardening alloys, particularly those of the Alnico type, permanent magnet alternators of larger output have become feasible for power applications. In some ratings these machines weigh less, require less space and cost less than d.c. excited alternators, owing to the absence of the d.c. exciter. Hence, these machines are assuming an important role for aircraft and other mobile applications in which the saving in weight is an essential feature. The design of permanent magnet machines presents several problems not encountered in the design of conventional machines. Outstanding problems are the effects of demagnetizing forces upon the permanent magnets, the shielding of the magnets from transient demagnetizing forces, and methods for reducing voltage regulation. In this connection it is important to recall that in a synchronous machine the armature-reaction magnetomotive force in the case of a short circuit may have an initial value five to eight times the full-load value and can therefore seriously demagnetize the magnets. The effect of transient demagnetizing forces may, however, be reduced greatly by casting the entire rotor in aluminium. This is equivalent to placing an infinite-bar cage winding on the rotor. The same general effect can be obtained by plating the poles with copper. The fact that no exciter is required in permanent magnet machines increases the efficiency. Thus a 15-kVA alternator of the permanent magnet type is claimed to have an efficiency of 90·9 per cent as compared with 82·5 per cent in the case of a conventional alternator.

● *SPECIAL ALLOY SOLVES ENGRAVING PROBLEM*

A firm of drawing instrument makers has found that an 18 per cent nickel-silver alloy solved a most difficult manufacturing problem. This problem consisted in the manufacture of 12 inch-long scales for a drawing instrument, the scale to be divided into graduations of 0·01 inch. The firm had made similar scales from various materials, but in all previous cases, only the first inch had to be marked off in such fine graduations. When confronted with the new task, the makers tried stainless steel first, but this caused considerable difficulties in trial runs, not only in machining the basis strip, but also in engraving each of the 1201 grooves about 0·005 inch deep. In addition, the life of the very small engraving tool of special alloy proved to be very short. Softer materials such as brass, aluminium, and plastics were also tried, but they did not possess the durability required. Also, it was found that grooves cut in aluminium were somewhat more difficult to clean and a small burr occasionally remained at the end of the cut or along the sides. This resulted in an irregular groove, hindering the next stage, which involved the application of



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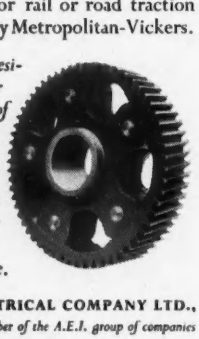
The severe shock loads imposed upon gear teeth when rail points or crossings are traversed at high speeds requires their design to be far more robust than normal industrial gears. Design and manufacture of traction gears must be directly related to the design of the traction motor. Metrovick experience in the production of traction motors therefore means that M-V gears incorporate all those features arising from practical traction needs. Solid, composite or resilient, spur or helical, all types of gears for rail or road traction can be supplied by Metropolitan-Vickers.



Metrovick rubber resilient gear wheel for a tramcar. Gears of this type protect motors against impact shocks and vibration, lengthen life of motors, gear wheels and pinions, while reducing noise.

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a small quantity of black pigment in each groove in order to make the scale easier to read. Finally, a free-cutting nickel-silver alloy containing 65 per cent copper, 18 per cent nickel, 16 per cent zinc and 1 per cent lead proved to be suitable. This alloy also has a desirable light colour and a low coefficient of linear thermal expansion.

● *GAS NETWORK ANALYZER*

The employment of electric analog computers for network analysis has now been extended to gas supply systems. Such a distribution system calculator enables one, in the space of a few hours, to perform calculations of flow distribution and pressures which by other means would require days or weeks of study. The calculator referred to was specially developed to meet the specific requirements of gas distribution systems and is claimed to be the first of its kind. The instrument is operated on direct current and its wattage is so low that it can be operated on ordinary dry batteries. It has resistance components sufficient to permit the simulation of 240 sections of pipe and has 120 load devices to represent concentrated loads at that number of points. Furthermore, it is equipped for making calculations based upon up to 48 points of supply. To set up a problem, a map of the distribution system is required; on it all pipe line diameters and lengths must be marked, and the gas quantities required per hour indicated at the points of demand throughout the system. A figurative model of the system is then set up on the calculator by connecting the resistors and load devices in the proper sequence. The resistors and load devices are then adjusted to represent the flow resistances, and gas consumptions respectively. After balancing the system, the current through the resistors will indicate the corresponding gas flow, and the voltage will indicate the gas pressure at the respective point.

● *SHOCK WAVES IN METALS*

The subject of shock propagation in engineering materials, particularly in metals, is highly important from the design aspect. Shock loading represents the exact opposite of static loading and consists in the instantaneous application of an external force to a structural member. In the case of two colliding bodies, the process of impact produces waves which are propagated from the point of contact both along the surface and towards the interior of the bodies with certain characteristic velocities, depending upon the pressure amplitude. Reflections of the waves occurring at the surfaces of the material will superimpose themselves upon the incident wave and the resulting complex stress system will then act on the metal. In tests made on metal cylinders subjected to a contact explosion, the free surface exhibited a pronounced bulge which, upon sectioning, yielded an extremely brittle failure with pronounced cracks normal to the direction of wave propagation. The particle velocity induced by the shock wave at the free side was measured by metal discs inserted loosely in a hole drilled into the cylinder. The velocity at which these discs were ejected was measured by means of high-speed photography, and the stress computed on the basis of theoretical considerations. In order to obtain more accurate information concerning the particle velocity at the free surface of such a cylinder, an optical technique has been devised which records the time history of the surface distortion, and thus yields particle velocity, by an extremely high-speed camera.

● *MULTIPLE-ENAMEL COATING FOR WIRES*

In many fields, the enamel insulation of electric wires has been largely superseded by the use of thin plastic coatings or by coatings of special types of glass fibre. Enamel-covered wire, however, still finds very wide application in the radio, television, and radar industries. To produce a reliable enamel coating, a very high degree of manufacturing skill is required, and any shortcomings in this respect will invariably result in a defective product. The usual method of applying the coating consists in pulling the wires through the enamel bath and then subjecting them to a baking process. Undesirable features of this process are claimed to be high process temperature, long processing time and uneven thickness of the coating produced. A more advanced method which is claimed to avoid the pitfalls and shortcomings of the older processes consists in the application of multiple enamel layers, each layer being baked prior to the application of the next. The new coating plant is claimed to produce a coating of uniform thickness with a lower process temperature than was hitherto required. Flexibility of the wire is therefore greatly improved. In this plant, the wire to be coated is passed repeatedly from the bath into the oven and back to the bath, and so on, the thickness of the individual coatings being controlled by a special device. Up to seven successive coatings can be produced in this way. The last coating stage is followed by the usual inspection by drawing the coated wire through a mercury bath.

● *BETTER COOLING OF POPPET VALVES*

A prominent British firm of motor car makers has invented a most effective device for intensifying the cooling effect of the sodium contained in the sealed hollow stem of the poppet valves used in internal combustion engines. In valves of this kind, the cooling liquid does not fill the cavity in the valve stem and is displaced by forces of inertia from end to end of the cavity as the valve reciprocates, so that it carries heat from the

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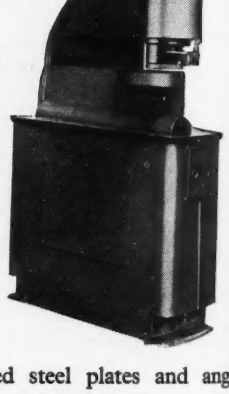

These machines are sturdy and robust, being constructed of fabricated steel plates and angle sections, electrically welded. Practically no maintenance is required, in fact all that is necessary is lubrication of the electric motor and occasional renewal of the blades. The top rate performance and fractional operating costs, together, with the sound design and construction, place this machine in a foremost position in the engineering world today.

Literature on this machine, and also on the Pilot Hydraulic Power Press is available and may be obtained on request from the makers,

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valve head to the valve stem, from which it is dissipated to the water-cooled valve guide. It has been proposed to produce a forced circulation of the cooling medium by placing a piece of tubing in the cavity in order to create a pumping action, but it has now been found that a more intense cooling effect can be obtained by using a rod as displacement member, the length of the rod being somewhat less than that of the valve stem cavity. The diameter of the rod is so chosen that sufficient clearance between it and the wall of the cavity is left in order to allow the cooling medium to pass through that clearance from one end of the cavity to the other in either direction, according to the motion of the valve. Needless to say, the high velocity of liquid flow in the clearance produces an excellent rate of heat transfer from the wall to the liquid, and vice-versa. This very simple device furnishes striking proof of the importance which may attach to design features which, on the surface, appear to be no more than minor details.

● *NOVEL CONTROL METHOD FOR RESISTANCE WELDING*

For many years past, a great amount of effort has been expended upon the development of the technique of resistance welding, the perfection of the welding machines, and improvement of the accuracy of the control equipment. One of the chief aims of development was to find a means of synchronising the three basic requisites of resistance welding, namely, magnitude of the welding current, electrode pressure, and duration of current flow. This synchronisation is important because even a small change within any of these variables, or a change in a number of other variables, may result in a defective weld with no warning to the operator. Some five years ago, the suggestion was made to utilize the welding current itself as a controlling force. This suggestion to establish a positive current-force-time relationship involved applying the electrode pressure through utilisation of the passage of the current through the primary and secondary windings of a resistance welding type transformer. It was calculated that pressure would be applied as current started to flow through the windings, and the time factor, therefore, would be a definite relationship to the current flow. This important development of applying pressure to the electrodes of a resistance welding machine by application of an electromagnet incorporated in the welder secondary circuit has now advanced from the laboratory to the production-tool stage. In this new welding process, the extremely accurate current-force-time relationship has made it possible to carry out very difficult and critical welding operations with a degree of uniformity hitherto unobtainable; and its application may therefore be expected to increase considerably in the near future.

● *MELTING METAL IN MID-AIR*

It is well known that the production of pure metals becomes increasingly difficult, the higher the degree of purity required. A striking illustration is the difficulty experienced in the production of high-purity metals of high melting point. Thus, for instance, when zirconium, vanadium, molybdenum, or titanium is melted in a crucible, there is inevitably some reaction with the refractory material of the crucible. Obviously, if it were possible to melt the metal without touching anything, except molecules of gas—as if done in a sufficiently high vacuum—high purity would be obtained. This veritable *tour de force* has now been realized by what has been termed the " float melting " method, which relies upon levitation of the metal body by electrodynamic forces. By placing a lump of metal between two slightly conical horizontal coils of copper spaced a few inches apart, and then passing an alternating current of, e.g., 10,000 cycles through the coil, the metal is lifted and becomes suspended in space between the coils. At the same time it begins to spin. Owing to the induction heating effect, the suspended metal mass becomes hot and finally melts, assuming the shape of a rapidly spinning toy top. So far, this process has been applied successfully to laboratory melts involving small samples, and to a very limited number of metals. Nevertheless, float melting has been employed to advantage in research for metals in lamps, electronic valves, and other high-temperature applications, where it is not necessary to carry the temperature of the metal beyond its melting point.

● *TIN ECONOMY BY DIFFERENTIAL COATING*

Important savings in tin can be achieved if the thicknesses of the tin coatings on the sides of the sheet are chosen in strict accordance with requirements instead of providing both sides with a coating of equal thickness, capable of withstanding the more severe conditions prevailing at one side only. This applies, in particular, to food containers, in which the inside coating must be of sufficient thickness to resist the attack of the product and the outside coating must be adequate to withstand the rigours of processing and atmospheric conditions. Differential coating on a commercial scale is now carried out in electrolytic tinning plants of the horizontal type which permit the deposition of tin on one side of the strip at a time. In the first experimental runs, a product was achieved carrying a 50/100 tin coating, i.e., the coating on one side was twice as heavy as that on the other side. When food containers made from this product were found satisfactory, full-scale commercial production was commenced, and one company now produces coatings in five combinations of thicknesses, the savings in tin effected

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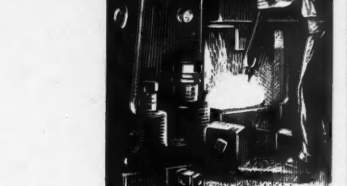

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ranging from 16·7 per cent to 50 per cent. A visible identification mark on each side of the sheet is, of course, essential, and a great many fast-drying printing media were tried until a palm oil ink was found to produce a satisfactory marking at coiling speeds of up to 1500 fpm. It is reported that the differential process may possibly be extended into the field of flash coatings of a few millionths of an inch thickness.

● *ELECTRIC MOTOR AND ALTERNATOR ANALOGS*

Two recent reports emanating from widely different and quite independent sources announce the development of electric devices for predicting or simulating the characteristics of electric motors and alternators respectively. One report announces the development of a computer of the analog type specially designed to simulate motor windings of the type used in fractional horse-power single-phase motors. With this computer, the performance of a proposed design which would probably require a week to calculate can be determined in less than a day. The computer was designed for single-phase instead of polyphase motors because the presence of starting winding and starting capacitor makes the computation of the single-phase motor much more complex than that of the polyphase motor. The other report is concerned with the development of a small laboratory machine to possess electrical characteristics corresponding to those of large power plant alternators. This requirement arose from the fact that, in order to obtain information upon some particular aspect of electric power system stability, recourse must be had to laboratory tests. Unfortunately, ordinary laboratory machines, while having steady-state characteristics reasonably similar to those of a large generating plant, have quite different transient characteristics. They cannot, therefore, be used for simulating transient short-circuit currents as they occur in large systems. The solution to the problem was found to lie in the use of a special type of amplidyne machine which is used to increase the transient time constant of a synchronous machine by injecting a voltage into the field circuit of the synchronous machine.

● *NOVEL COLD-FORMING PROCESS*

A proposed novel method for cold-forging consists in subjecting the cold metal blank to be shaped to rapidly increasing pressure in order to bring it into the plastic state. While this process is taking place, energy is stored up in a resilient member of the press mechanisn which is automatically released when the blank has reached the plastic state. This stored energy is then used to effect the flowing of the plastic metal into the required shape. If desired, the volume of metal displaced may exceed 10 per cent of the total volume of the blank. It is claimed by the inventor that this method resembles more closely shaping operations hitherto performed wholly or partly by hot-forging than any cold-shaping operation employed so far. The method should make it possible to manufacture many kinds of metal articles without the necessity of preheating and without the machining operations previously required either to obtain a more intricate shape or the required surface finish. The inventor states that the grain structure of articles formed by this process exhibits flow lines which closely resemble those obtained with hot forging. It should therefore be possible to displace the metal in such a way as to obtain maximum strength of the formed article in given directions. The patent referred to gives interesting examples of cold-forming operations carried out by the new process and it also describes not only the design of the resilient energy storing device to be employed but also that of special machines incorporating the invention.

● *FLOWMETER WITH ELECTRONIC INTEGRATION*

A new type of flowmeter has been developed which incorporates an evenly graduated scale and electronic integration. The new metering device is claimed to possess fast and accurate response over the entire operating range, as well as to be low in initial cost and maintenance. It is built either as a mechanical flowmeter with integral recorder, or as an electrical transmitter used with an inductance bridge receiver. The heart of the evenly graduated meter is a bell of special contour which senses the differential pressure created by the fluid to be measured, which is passed through an orifice. This bell consists of a hollow float, the inside surface of which is shaped to give a square root response to pressure changes. The electronic integrating mechanism comprises three major assemblies, namely, (a) a scanning unit which ascertains the flow rate every five seconds, as shown by the meter pen, (b) an electronic detector relay actuated by the scanning unit which operates the counting mechanism and (c) a motor-driven counter assembly which totalises the successive output impulses from the detector relay. In the scanning unit, a lightweight vane is positioned by the meter pen through a mechanical linkage and dual oscillator coils mounted on an arm driven by a precision cam. When the leading edge of the vane is intercepted by the coils, the electronic detector relay is energised, permitting the counter motor to operate.

For further information on developments reviewed in THE BLUE PAGES, readers are invited to write to the Editor.

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Conservation of Strategic Materials in Germany*

By K. P. HARTEN. (From *Metal Progress, U.S.A.*, Vol. 60, No. 5, November, 1951, pp. 58-63, 5 illustrations.)

To ensure that proper use is made of the metals available, the co-operation of the consumer is essential. He chooses materials in such a way that purchase and maintenance will represent a minimum cost. Before switching over to new materials, the consumer requires adequate data on their performance. Frequently, a decision will be easier to reach and conservation will be more rapidly ensured if dependable short-time tests are available, so that it is important to develop tests of this type.

The user will realise, for instance, that a high-speed steel should not be selected according to the most severe conditions of operation encountered, if these cover only a small proportion of the actual applications.

Extensive tests have led in Germany to the decision to make considerable use of the ABC-III type steel. The following table gives the performance for roughing and finishing cuts of five common grades of high-speed steels and the cemented tunsten carbide S-1.

Material	ABC-III 2·5% Mo 2·5% V 2·5% W	B-18 1% V 18% W	D-Mo-5 5% Mo 2% V 6·8% W	E-Co-3 1% Mo 2% V 12% W 3% Co	E-18-Co-10 1% V 18% W 10% Co	S-1 74% W 6% Co
PCS†:						
Finish cuts	28	29	29·5	31	32·5	23
Roughing cuts	12·5	13	14	16	18·5	—
P/UT††:						
Finish cuts	9·5	1·7	4·5	2·6	2·0	4·2
Roughing cuts	4·3	0·8	2·2	1·3	1·24	1·8

†PCS = permissible cutting speed for 0·60-0·65% carbon steel.
††P/UT = performance related to the unit of tungsten content.

The ABC-III steel is frequently the most favourable to use from the standpoint of plant economy. However, it is not suggested that the material with the lowest alloy content will always be the most economical. For example, the cemented carbide S-1, despite its high tungsten content of 74 per cent, yields a higher performance per unit of tungsten than high-speed steels with 12-18 per cent tungsten. The superiority of the tungsten carbide is even more evident at higher speeds. This is why the S-1 alloy is used in Germany as far as modern machine tools available will permit its economical use.

Extreme adaptation of the material to the particular use involved, in the interests of conservation of strategic materials, may have, however, the undesirable effect of increasing the quantities of material in stock and the number of special treatments required, so that the extent to which adaptation should be carried out requires careful study in each case.

Conservation in processing and designing. Sometimes it will be found economical to use a method of processing requiring a higher consumption of lower-grade materials. This usually applies to tools, which can be made either entirely of high-alloy steel or as composites, with working edges welded or inserted in a body of plain or low-alloy steel. However, the additional manufacturing costs of the composite substitute must not become excessive. Considering, for instance, stainless steel sheet, it may be said that clad metal fabrications should usually be avoided below certain thicknesses of the order of 0·20 in.

Standard specifications should be examined to see if they do not stipulate safety allowances and, therefore, weights and alloy contents which are unnecessarily high, particularly under the present emergency conditions. It has been found, for instance, that the actual values for the creep limit of German tube steels are generally 20-25 per cent higher than the guaranteed DVM creep strength values for tube steels 15Cr4, 15Mo3, 13CrMo44, and 8CrMoV9. Rules for steam boilers and high-pressure containers should also be given further examination. It would be most helpful if an interchange of experience in this respect could be achieved between the coding authorities of various countries.

Scrap should, wherever possible, be separated into groups or grades by the processing firms, before sending it to the central collection depots, in order to recover alloys which would otherwise be lost in mixtures of unknown composition. Alternatively, an exact analysis of the scrap after melting may be well worth the trouble.

Conservation in steel production. Deoxidation and alloy agents high in manganese are being used as sparingly as possible in Germany. Only the manganese carrier with the lowest content of manganese which will yield the desired carbon and manganese in the finished product is used. For example, in smelting basic bessemer pig iron (0·6 per cent manganese), no ore is used having more than about 2 per cent manganese.

For deoxidation of basic bessemer steel containing ordinarily more than 0·05 per cent carbon and 0·3-0·8 per cent manganese, and of open hearth steels with more than 0·12 per cent carbon and 0·6 per cent manganese, only ferromanganese with a maximum of 45 per cent manganese is allowed. Preliminary deoxidation with spiegel or blast-furnace manganese-iron containing 20-30 per cent manganese is carried out. To keep melting losses as low as possible and to obtain the maximum benefit, deoxidising alloys are added in the ladle rather than in the furnace.

Special slag reducing methods have been developed for the recovery of chromium. At the end of the refining action, when a fairly important part of the chromium has entered into the slag, calcium-silicon and ferro-silicon (sometimes with some aluminium) are

*From an address before the opening session of the First World Metallurgical Congress, Detroit, Michigan, U.S.A., October 14, 1951.

added. A white slag with a low carbide content is thus obtained, from which much chromium will be transferred back into the metallic bath. With this method, large amounts of high phosphorus slags must be skimmed previously. Mention should also be made of the known method of working of stainless chromium-nickel scrap with an oxygen lance in the electric furnace, which enables almost all the nickel, and 80-90 per cent of the chromium, to be recovered.

Vanadium can be recovered in considerable quantities from basic bessemer pig iron. In the basic bessemer slags or in the roof dust of steel plants, the concentration of vanadium is 0·4-0·6 per cent. To minimise converter losses, preliminary refining slags with 2-4 per cent vanadium can be obtained in the blast-furnace trough or in ladle-like vessels by blowing in hot air or oxygen. In view of the general shortage of this alloying element, the cost of the vanadium is considered justified.

Substitution of alloys low in strategic materials. Cobalt can be dispensed with only rarely without impairment in quality. For instance, experiments with cemented carbide tools with nickel as a binder show a decrease in performance.

Columbium is essential for high creep strength alloys, but can be replaced by titanium in stainless steels. In many cases, the troublesome intergranular corrosion can be avoided, without adding either columbium or titanium, by sufficiently reducing the carbon content of the stainless alloy.

Molybdenum in high speed-steels can be replaced by tungsten, but the latter is in even greater demand. Although the replacement of molybdenum by vanadium alone or in combination with chromium or aluminium is not yet possible, the ABC-III steel, mentioned previously, is a very useful substitute high-speed steel. For creep-resistant steels, part of the molybdenum can be replaced by vanadium although this requires higher heat-treating temperatures. The substitution cannot be recommended for heavy forgings such as steam turbine rotors and heavy shafts, since these must be stress-relieved by gradual cooling without suffering from temper brittleness. In conventional heat-treating and case-hardening steels, the use of boron has been an important development. For stainless steels, no adequate substitute is available for molybdenum, although replacement of stainless steels by 16 per cent silicon cast iron or ceramic bodies is sometimes possible.

Nickel can be eliminated or at least used much more economically in case-hardened and heat-treated steels. For heavier sections, however, its deep-hardening effect cannot be attained with other elements such as manganese. In stainless steels, manganese and nitrogen

(*Concluded on page* 49)

BELGIUM

Electronic Control of a 120-ton Travelling Crane

By L. BARBIOT and R. VANSTEENKISTE. (From *Revue ACEC Charleroi*, No. 4, 1951, pp. 14-21, 17 illustrations.)

BEFORE describing the electronic control of the 120-ton travelling crane at the S.A. des Ateliers Metallurgiques de Nivelles, it may be useful to outline briefly the principle of operation and the characteristics of thyratrons, which are the electronic valves used in this equipment.

The thyratron is an electronic valve comprising a hot cathode, an anode and a grid ; it is filled either with a gas or, more commonly, with mercury vapour. It is an electric rectifier, conducting only in the anode-cathode direction. Conduction is indicated by ionisation of the gas ; this process, called " ignition," occurs only when the potential of the grid with respect to the cathode acquires a certain value.

A simple analogy, namely, comparing the thyratron with a water valve (Fig. 1), will facilitate understanding of the principle of its operation. The bolt *G* corresponds to the grid, the water pressure representing the difference of potential in the anode circuit, and the flow of water the flow of current. If, at a given instant, the bolt *G* is withdrawn, the valve is unlocked and, due to the water pressure, will turn instantly from position 1 to position 2 ; owing to the water flowing in the pipe, it will remain permanently in this position, and no possible operation of the bolt will, at this stage, affect the flow of water. If the flow is to cease, the pressure must be removed for a short instant and the valve must again be locked in position 1 by the bolt *G*. The operation of the thyratron is similar. If the rider of the grid control potentiometer is entirely on the left

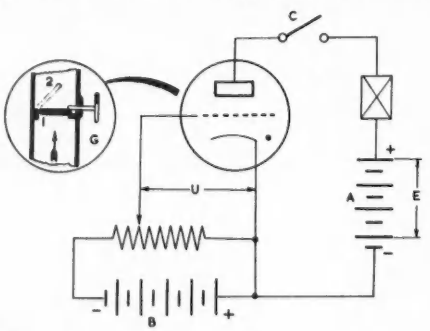


Fig. 1. **Schematic diagram of thyratron operation.**

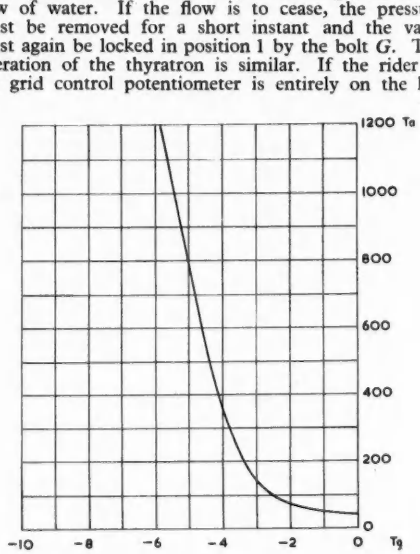


Fig. 2. **Curve showing grid voltage** *U*.
T_a = anode voltage, T_g = grid voltage for ignition.

(the grid potential highly negative), and the switch C closed, no current will flow through the tube. When the grid is made progressively less negative, a stage will be reached when ignition will take place and the full conduction current will flow instantaneously. The voltage drop between anode and cathode during conduction, of the order of 15 V, will remain constant and unaffected by the resistance of the external circuit. If the rider of the potentiometer is then returned to its original position, the current will nevertheless continue to flow at an unchanged rate. The only means of interrupting the current is to open the switch C, for a short instant at least. The grid potential U required for igniting the valve is a function of the anode voltage E (Fig. 2); the area to the left of the curve corresponds to an unignited valve and that on the right to an ignited one. If, for instance, the anode voltage is 600 V and the potential of the grid —6 V, no current will flow when the switch C is closed; to initiate conduction, it will be necessary to reduce the grid potential to —5 V or less. It is sufficient for this potential to be established for only an extremely short instant.

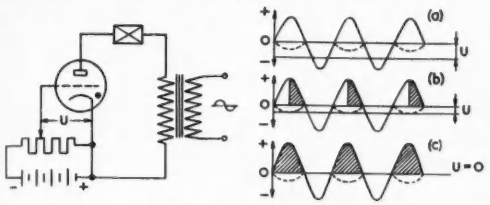


Fig. 3. **Method of changing the ignition point.**

If a variable voltage is applied to the anode, the grid potential required for ignition will also vary. Fig. 3 illustrates this relationship when a sinusoidal alternating voltage is applied to the anode; the dotted wave represents the grid voltage required, on the basis of Fig. 1, to initiate conduction corresponding to the instantaneous value of the anode voltage, and is called the " dynamic ignition characteristic " of the valve.

In the case (a), Fig. 3, the (negative) grid voltage applied is greater than the (negative) maximum of the characteristic and, consequently, ignition does not take place. If the grid voltage is gradually reduced, ignition will take place as soon as the grid voltage equals the maximum of the characteristic; no current will, in this case, be flowing during the first half of the semi-cycle, while from the peak onward, the full conduction current will be flowing for the rest of the semi-cycle. During the following semi-cycle, the anode voltages being negative, no ignition will take place and consequently no current will flow. When the grid voltage is made zero, ignition will take place at the beginning of the positive semi-cycle and the full conduction current will flow throughout this period. By controlling the grid voltage, it is therefore possible to control the mean conduction current between its maximum and half this value. In practice, however, the method of regulation described is not used, mainly because of inadequate stability and because it is effective only over half a semi-cycle. One of the most widely used methods of regulation consists in superimposing on the variable d.c. voltage U applied to the grid an alternating voltage V of constant amplitude, lagging 90 degrees behind the anode voltage E (Fig. 4).

For example (a), Fig. 4, the resulting grid voltage is always greater than that corresponding to the maximum of the characteristic, and no ignition takes place; examples (b) and (c) correspond to increasingly longer periods of conduction. By this means it is possible to vary the output of the thyratron continuously between zero and the maximum. Figs. 5 and 6 show the simplified circuit diagrams of a half-wave and full-wave variable voltage thyratron rectifier respectively.

Fig 6 shows a schematic circuit diagram of the electrical equipment of the travelling crane. The Ward-Leonard set consists of an asynchronous motor MA driving a dynamo G. The latter may feed, through switches C_1 and C_2 either the hoisting motor MH or the traversing motor MT. Electrical interlocking by means of auxiliary contacts on the two switches precludes the simultaneous operation of both motors. The excitation of either motor MT or motor MH is supplied from a constant voltage grid-controlled thyratron rectifier EM.

The excitation of the dynamo is supplied from another thyratron rectifier EG, the output voltage of which can be varied continuously by a potentiometer controlling the grid voltage. Advantage is taken of the great efficacy of this system by adding an automatic device for limiting the output current of the dynamo. This employs a special transformer (Fig. 7) which has a primary winding connected to the mains, a secondary winding, and a tertiary winding which is connected in series with the dynamo armature. When no current flows through this winding, the limb AB of the magnetic circuit of the transformer short-circuits the primary flux, with the result that no e.m.f. is induced in the secondary winding. When a current flows through, the tertiary winding becomes correspondingly saturated, the secondary receives a larger proportion of the flux produced by the primary winding, and, consequently, an e.m.f. is induced in the secondary winding. When the current flowing through the tertiary winding increases, this e.m.f. increases correspondingly. After rectification and smoothing, it is applied to a potentiometer, to which a cold-cathode discharge tube N

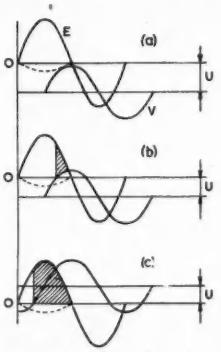


Fig. 4. **Basic principle of various methods for changing the ignition point.**
U = d.c. component of grid voltage.

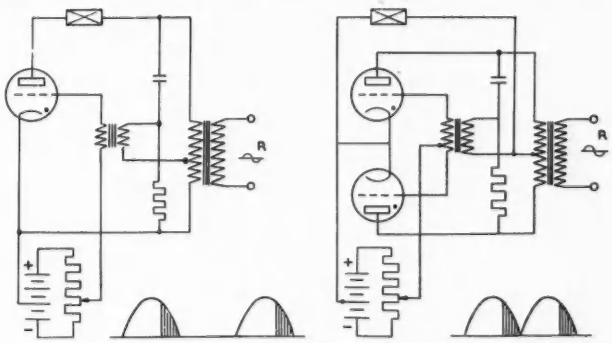


Fig. 5. **Circuit diagram of half-wave and full-wave thyratron rectifier.**

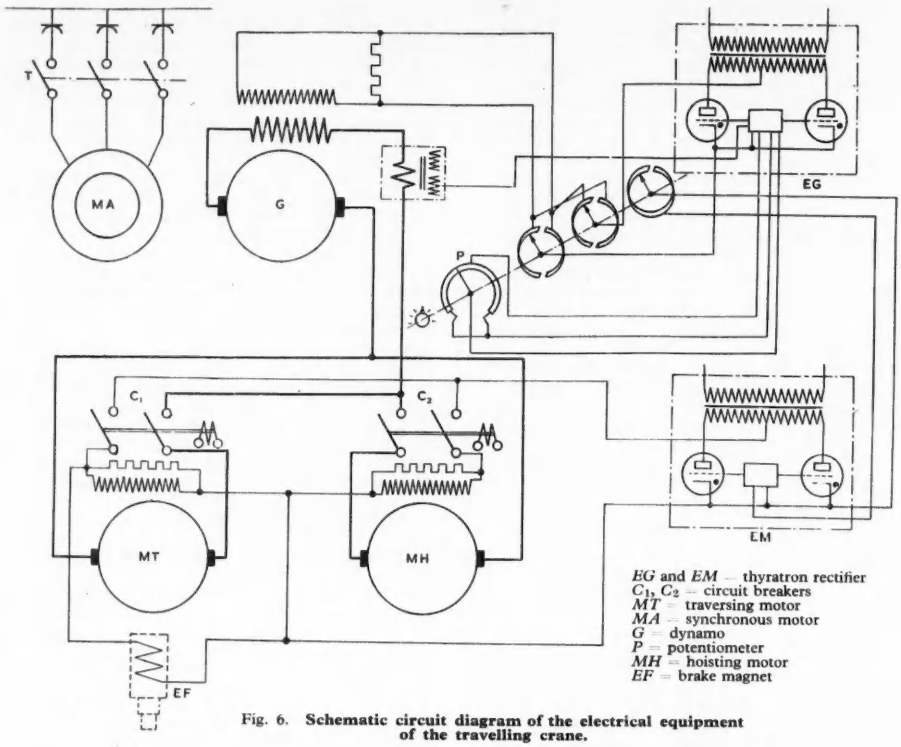


Fig. 6. **Schematic circuit diagram of the electrical equipment of the travelling crane.**

and a series resistance R are connected as shown in Fig. 8. This lamp becomes conductive only when the voltage applied at its terminals exceeds a definite value. The d.c. voltage on the terminals of the potentiometer being a function of the output current of the dynamo, the rider can be set so that a discharge occurs when the dynamo current reaches a certain value. The voltage appearing at that instant across the resistance R can be applied to the control grid of the thyratron in such a manner as to make it more negative with respect to the cathode. The excitation of the dynamo is thereby reduced and the main output current decreases until a value is reached at which the discharge in lamp N ceases.

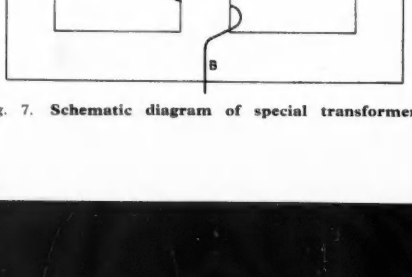


Fig. 7. **Schematic diagram of special transformer.**

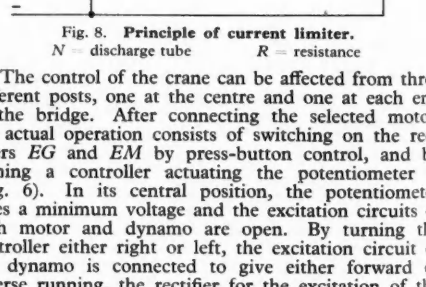


Fig. 8. **Principle of current limiter.**
N = discharge tube R = resistance

The control of the crane can be affected from three different posts, one at the centre and one at each end of the bridge. After connecting the selected motor, the actual operation consists of switching on the rectifiers *EG* and *EM* by press-button control, and by turning a controller actuating the potentiometer P (Fig. 6). In its central position, the potentiometer gives a minimum voltage and the excitation circuits of both motor and dynamo are open. By turning the controller either right or left, the excitation circuit of the dynamo is connected to give either forward or reverse running, the rectifier for the excitation of the motor is unlocked, and by turning the controller progressively, the excitation of the dynamo is correspondingly increased. The controllers of the three posts are mutually interlocking to prevent the operation of the crane from any post when the controller of a remaining post is not in its neutral position.

A Multi-Voltage Universal Motor

By A. NOVAZZOTTI. (From *Marelli Review*, Vol. 24, 1950, No. 1-12, pp. 24-27, 5 illustrations.)

COMMUTATOR motors of up to about 1 hp are commonly designed to work either on d.c. or on a.c. of 42 to 60 cps, but only for a single voltage. Special applications, however, often require motors capable of working on several different voltages, and thus for one of these applications, namely, the operation of calculating machines, a motor has been developed which can operate on 110, 160, 220 or 280 volts, at frequencies of 42, 50 and 60 cps, and on d.c., i.e., on practically all mains voltages and frequencies in common use.

In such a motor it is necessary, for reasons of simplicity in design, that all the possible changes in supply voltage should be completely compensated by the impedance of the stator winding, so that the armature voltage, which is calculated for the lowest operating voltage, can remain without change. The windings must further be designed to ensure the best utilisation of the motor with an acceptable current density, while the values of the induced voltages must be sufficiently low to ensure that the commutation voltage remains the same whatever the supply voltage used.

To ensure a satisfactory performance in d.c. as well as a.c. operation, the torques in both cases should be as nearly equal as possible, as equal torques will usually produce equal speeds and equal outputs. As is known, the torque of a d.c. motor is :

$$T_{dc} = \frac{0{\cdot}0162\ \Phi N I p}{10^{-8}} \quad [\text{kgm}]$$

where I is the total armature current, p the number of poles, N the number of armature conductors, and Φ the flux per pole. In the case of the motor operating on single-phase a.c., the torque is given by the same equation, if I and Φ denote the effective values of current and flux respectively, and if φ is the phase angle between the flux and the armature current :

$$T_{ac} = T_{dc} \cos \varphi$$

In practice, however, the actual torque is always smaller than the theoretical, as the brush axis is set, not at right angles with the flux, but at an angle φ, to compensate the armature reaction as far as possible and to improve commutation. The actual torque is then

$$T_{ac} = T_{dc} \cos \varphi \sin \alpha$$

From the above formulae, the speed can be derived for a.c. and d.c. operation respectively, and it will be found that

$$n_{ac} = n_{dc} \cos \varphi$$

To approximate the speed of the motor on a.c. and d.c. operation, it is necessary to reduce the phase angle φ between the terminal voltage and the current taken by the motor, which can best be done by keeping the counter-e.m.f. as low as possible. In fact, on starting, the counter-e.m.f. is zero and the phase angle φ is at a maximum.

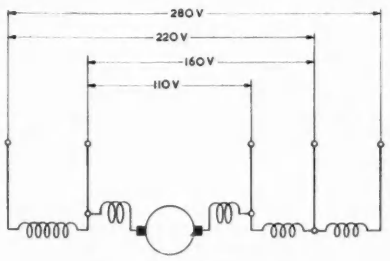


Fig. 1

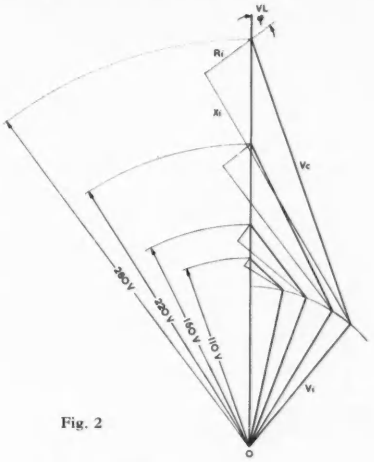


Fig. 2

Considering the previous general notes and Fig. 1, the principle of the multi-voltage universal motor can be simply explained. As previously stated, all changes of voltage must be compensated by the impedance of the stator winding. Taking as an example a motor designed for four different voltages, the vector diagram of the armature and stator voltages is shown in Fig. 2, VL being the mains voltage, Vc the voltage drop in the stator winding, Vi the voltage on the armature, Ri the voltage drop due to the resistance of the stator winding, Xi the voltage drop due to reactance of the stator winding, φ the phase angle between the voltage at the terminals and the total current. The diagram shows the voltage on the armature to be constant, the locus of the points of the four vectors lying on a circle, with the origin O as the centre. The terminal voltage VL is then the vector sum of the armature voltage Vi and the stator voltage Vc. The latter equals the vector sum of the voltage drops in the resistance Ri and the reactance Xi respectively. If the individual stator windings are laid out in this manner, the motor can be used for the four voltages indicated.

The angle φ is greater for an applied voltage of 280V and smaller at 110V, and thus the counter-e.m.f., in the

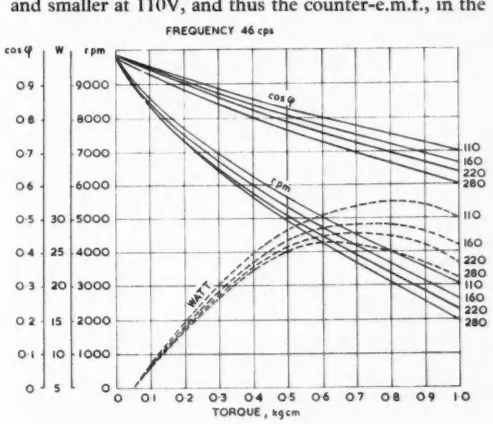


Fig. 3

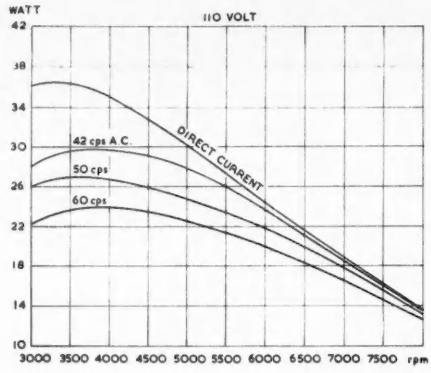


Fig. 4

case of operation on 110 V is proportionately greater than in the case of 280 V. Consequently, the motor is better utilised at 110 V than at 280 V. Fig. 3, plotted from test data, illustrates the characteristics described.

It might seem desirable to increase the e.m.f. of rotation by increasing the number of armature conductors ; this, however, would lead to an increase in the commutation voltage and a consequent shortening of the life of the brushes as well as that of the commutator. Fig. 4, shows the characteristics of a motor of 20W capacity, 6000 rpm, when operated at various frequencies. It will be seen that the motor should be used preferably at a speed of not less than 4000 rpm. A difference in speed—and hence in output—is unavoidable for the different types of supply. At 8000 rpm, the difference between operation on d.c. and a.c. of 60 cps is less than about 5 per cent ; at 7000 rpm it is about 12 per cent, and at 6000 rpm it is 17 per cent. Italian Standards allow a variation in speed of 17 per cent for motors with series excitation.

In special cases where the speed must be rigorously maintained on the occurrence of changes in voltage, frequency, and load, the motor can be fitted with an automatic centrifugal control which ensures that the required speed is maintained.

SWITZERLAND

The "Ipsotherm" Motor Protection

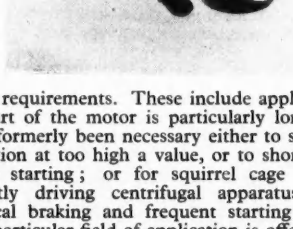
By S. E. HOPFERWIESER. (From *The Brown Boveri Review*, Vol. 38, No. 7/8, July/August, 1951, pp. 203-207, 9 illustrations.)

THERMAL protection of electric motors can be effected by separately mounted protective devices, through which the motor current flows ; alternatively, protection may be effected by devices mounted integrally with the motor end actuated either by a combination of motor current and motor temperature, or by motor temperature alone. The latter method is generally to be preferred for motors operating intermittently or on fluctuating loads. However, none of the devices hitherto developed has been suitable for universal application ; they have been either too complicated in use or not sufficiently reliable, or else they have been inaccurate or too expensive.

The " Ipsothermostat " has therefore been designed to satisfy the need for an integral thermal control element suitable for the widest application. Despite its simplicity and very small dimensions, several years of development work were required to bring it to perfection. It consists of a small metal casing provided with an insulated contact which encloses a curved bimetallic disc. Several such casings must be mounted between the end connections of the coils on both sides of the motor. If any part of the winding becomes overheated, the disc of the nearest thermostat will snap, close the contact and establish an auxiliary circuit which will disconnect the motor.

" Ipsothermostats " can be selected for trip temperatures of between 90 and 175° C, their thermal inertia is small, and the time lag between the winding reaching a critical temperature and the operation of the thermostat is correspondingly small. The difference between the trip temperature and the temperature at which the disc returns to its original position is about 15° C, allowing a motor which had been disconnected to be reconnected within a few minutes.

Despite its small size, the flash-over voltage of the contact is 1000V and the current-rupturing capacity at low alternating voltages is up to 40 A. The life of the thermostat is up to 80,000 switching operations. The " Ipsotherm " is particularly suitable for three-phase motors and it is intended to replace the ordinary overload protection in cases where the latter cannot



satisfy requirements. These include applications where the start of the motor is particularly long, and where it has formerly been necessary either to set the thermal protection at too high a value, or to short-circuit them during starting ; or for squirrel cage motors intermittently driving centrifugal apparatus ; or where electrical braking and frequent starting are required.

A particular field of application is offered by motors where the internal connections are changed during operation, as, for instance, in multi-speed squirrel cage motors and where the nominal motor current may have various values. It may also be useful for protecting motors when the cooling is inadequate due to dust in the air passages, unsuitable location, failure of the cooling fan, etc.

When the motor must not be stopped immediately on the occurrence of overload because this might be detrimental to the work, for instance, in welding, the " Ipsotherm " may be used in conjunction with an alarm circuit, and a second element, set for operation at a slightly higher temperature, can be used for interrupting the supply if the warning signal is overlooked.

Electric Arc Welding of Magnesium Alloys

By K. K. KHRENOFF, M. N. GAPCHENKO, and G. P. SAKHATZKY. (From *Avtogennoye Delo*, No. 8, 1951, pp. 1-5, 8 illustrations.)

The authors describe tests on the carbon arc welding of corrosion-resisting magnesium alloy. Recommended practice covers the use of fluxes, welding procedures and welding conditions, and preparation and positioning of components. Mechanical and metallographic tests and general inspection lead to the conclusion that all types of joints can be carried out, giving good appearance and sound welds, but somewhat lower mechanical properties. Subsequent heat treatment improves uniformity, but not mechanical strength.

INVESTIGATIONS were carried out on the technique of electric arc welding of magnesium alloy MA1. This alloy contains 1·3 to 2·5 per cent manganese and some impurities, and is distinguished by its corrosion resistance. In wrought and extruded form, this alloy has the following mechanical properties (mean values): Yield strength 12 kg/mm², ultimate tensile strength 21 kg/mm², elongation 8 per cent, Brinell hardness 45.

Magnesium alloy MA1 can be satisfactorily gas-welded, but cracks often appear in the seam.

These investigations deal with d.c. and a.c. carbon arc welding. In d.c. welding, a series resistance was included in the circuit.

Electrodes used had a cross-section of 100 to 150 mm², the ends being pointed. For additional material, filler rods of 1·5 × 3, 2 × 4, and 2·5 × 5 mm cross-section cut from the sheets to be welded were used. These rods and the edges of the sheets were cleaned mechanically by scraping off the layer of oxide until a metallic lustre was obtained.

Fluxes were diluted with water to the consistency of a thin cream and were applied to the metallic surface by means of a brush. In some cases, artificial drying in an electric oven followed the application of flux.

Samples for experimental welding were cut from sheet of 0·8, 1·5, 2·0, and 2·5 mm thickness. The sheets were 100 to 200 mm in length, and 75 to 100 mm in width. Welding was carried out in special fixtures allowing of clamping samples in position. Butt joints were made with the samples backed by plates of stainless steel. Freedom for forming a raised welding seam on both sides of the sheet was allowed in the fixture.

After welding, samples were washed in warm water, cleaned with a steel brush and dipped in an oxidising solution for five minutes at 70-80° C, then washed in water again and dried. The oxidising solution contained $K_2Cr_2O_7$—2%; HNO_3—3%; NH_4Cl—0·1%.

A number of fluxes were tried. The list given below contains fluxes used in the smelting of magnesium alloys (1), (2), (3), and fluxes used in gas-welding of magnesium and aluminium alloys (4) to (10).

(1) $MgCl_2$—KF. (2) $MgCl_2$—KCl—NaF. (3) $MgCl_2$—KCl—MgF_2. (4) $MgCl_2$—LiCl—KF—NaF. (5) $MgCl_2$—LiCl—KCl—KF—NaF. (6) LiCl—KF. (7) LiCl—KCl—NaCl—NaF. (8) LiCl—Na_3AlF_6. (9) LiCl—NaCl—Na_3AlF_6. (10) KCl—NaCl—Na_3AlF_6.

Best results were obtained with flux systems (4), (5) and (6). These ensure good welding, neat formation of the seam, and a clean surface.

In the course of this investigation, the percentage of the constituents in each flux system was varied. In arc welding, the best fluxes are those containing more than 50 per cent of fluorine compounds. However, an increase in the percentage of flourine compounds requires a higher welding current. Fluxes must be prepared on the day of use. Metallic surfaces should be covered with flux immediately before welding, and if this is done, no drying is necessary. With oven drying, the flux can be brushed on some time before welding. In this case, however, the welded components must be kept in the oven until welding. At room temperature, the flux collects humidity and components covered with flux corrode.

When covering the edges of components with flux, no gaps must be left, and uniform coverage must be ensured. Excessive thickness of flux layer is useless. Coverage must extend 8-15 mm beyond the seam (depending on sheet thickness). The mean consumption of flux is 6 grams per running metre of seam for the component, and 5 grams per metre length of filler rod of 2·5 × 5 mm cross-section. Investigations into the properties of fluxes and into the selection of welding procedures were carried out with joints of all types (see Fig. 1).

With a good flux, high-quality joints of any type can be obtained in sheet from 1·5 to 2·5 mm thickness. Sheet of 0·8 mm thickness requires considerable skill, owing to the risk of burning through the material.

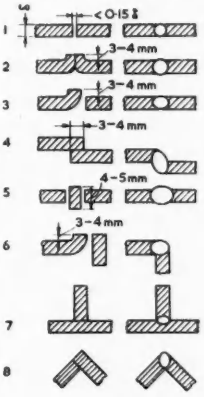
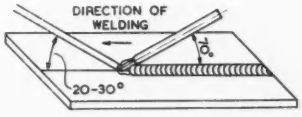


Fig. 1 (left). **Types of joints for arc welding magnesium alloys with carbon electrodes.**

Fig. 2 (below). **Diagrammatic sketch of butt welding thin sheets with additional material supplied by filler rods.**



Butt joints require additional material in all thicknesses of sheet. Figure 2 shows some details of procedure which should be observed in welding. To obtain an increased thickness of weld, this process must be carried out on a backing plate grooved under the seam.

In welding, the " flame " of the arc is directed onto the edges, where a metal pool is created, into which the end of the filler rod is dipped. The arc is advanced along the seam with a speed determined by the melting of the filler rod. This procedure ensures a satisfactory appearance of the seam. The seam is proud on both sides. For improved forming of the seam, the support must be painted with flux before welding.

Sheet of about 2 mm thickness can be welded without filler rod, but the seam is then starved, and this procedure is not recommended. When the thickness is 2-2·5 mm, additional material can be applied in the shape of a strip placed between the edges to be joined (Joint No. 5, Fig. 1). A thick joint is obtained in this way. Edge, lap and corner joints can be executed without additional material, with no detriment to the quality of the joint in any thickness of sheet.

The welding of Tee joints is difficult and requires special skill. Additional material is essential. By melting the vertical wall to a sufficient depth it is possible to get a two-sided fillet effect, although welding takes place on one side only. To obtain this result,

welding must be carried out while the component is placed edge downwards.

Welding can be carried out with d.c. of straight polarity, as well as with a.c. The authors obtained a better appearance of the seam with d.c. The only disadvantage of d.c. welding is the observed drifting of the arc, but an arc of 2·5-5 mm remains stable. In a.c. welding, the drifting of the arc is less pronounced, but the general stability of the arc is reduced.

The welding procedure was developed by trial and error. For each type of joint, an optimum current was determined, which assured normal penetration and a good appearance of the seam. Maximum allowable speed of advance at the given current was then found. In all cases, welding was carried out with a short to medium length of arc (2·5-5 mm). Optimum welding conditions with d.c. are listed in Table I.

TABLE I. WELDING CONDITIONS FOR D.C. ARC WELDING OF MAI ALLOY WITH CARBON ELECTRODE.

Thickness of material [mm]	Type of joint	Welding conditions*		
		I_s [A]	I_w [A]	v [cm/min]
1·5	Butt joint with additional material ..	120	75	22
2·0	Same	122	78	22
2·5	Same	128	95	17
0·8	Lap joint without additional material ..	20	15	55
1·5	Same	122	78	47
2·0	Same	122	87	66
2·5	Same	128	95	35
0·8	Edge joint without additional material ..	20	15	55
1·5	Same	122	78	48
2·0	Same	122	87	66
2·5	Same	128	95	35
1·5	Corner joint without additional material	122	78	48
2·0	Same	122	87	66
2·5	Same	128	95	35
1·5	T-joint with additional material ..	124	78	18
2·0	Same	130	100	16
2·5	Same	130	100	14

*I_s = short circuit current, I_w = the working current, and v = speed of welding.

Special attention must be given to the preparation of the edges and to the positioning of the components. The edges must fit tightly along the whole seam. Gaps cause holes, particularly in the case of a sheet thickness below 1·5 mm.

Metallographic examination of the seams has shown a satisfactory condition of the metal in all types of joints, provided the flux has been effective. However, the crystal structure of the metal varies in different zones of the seam. The metal solidified after melting has a pronounced coarse grain structure. In the zone adjacent to the solidified metal, the heat treatment resulting from the welding process also produces a coarse grain structure.

The following homogenising heat treatment was applied to the samples. Heating temperature : 450° C, held for two hours, cooled off together with the oven in sand. This treatment caused an appreciable modification of the microstructure in all the zones of the welded seam.

The welded joints of the samples were subjected to mechanical tests for hardness, tension and bending.

The Rockwell B hardness of different zones of the seam on as-welded and heat-treated samples is shown in Table II.

We observe that the hardness varies across the seam. The lowest hardness is found in the zone of thermal effect, close to solidified metal. Subsequent heat treatment produces a more uniform hardness, as well as a more even microstructure.

Tensile strength tests were carried out on butt and lap joints, both with raised seams and following the

TABLE II. HARDNESS IN DIFFERENT ZONES AROUND THE SEAM.

Test point	Mean Rockwell B hardness	
	Samples as welded	Samples heat-treated after welding
Centre of seam ..	50·5	46
Edge of seam	42·0	48
2 mm from seam ..	38	48
4 mm from seam ..	43	49·5
6 mm from seam ..	45	49
8 mm from seam ..	45	47
20 mm from seam ..	46	47·5

removal of excess material. Samples of 15 mm width and 150 mm length were used. Results of tensile tests on as-welded and heat-treated samples are given in Table III.

The table shows that the strength of welded joints averages 15 kg/mm², in other words, it lies below the strength of the material. The failure of all samples with excess material in the seam takes place in the thermally affected zone alongside the seam. After removal of excess material, failures also occur in the seam. Lap joints always fail in the seam. Heat treatment makes no appreciable difference to the tensile strength of joints.

TABLE III. U.T.S. (kg/mm²) OF SAMPLES TESTED AS WELDED AND AFTER HEAT TREATMENT.

Thickness [mm]	Butt joints		Lap joints	
	as welded	after heat treatment	as welded	after heat treatment
0·8	9·8—9·5 9·7	9·9—9·8 9·85	10·9—6·0 8·6	11·0—6·5 8·7
1·5	15·8—15·2 15·6	17·3—15·6 15·7	15·1—10·4 13·2	15·5—10·4 13·55
2·0	16·0—13·2 15·6	15·78—11·8 9·8	11·8—9·8 10·8	11·9—9·5 10·8
2·5	18·2—16·4 17·4	18·8—16·4 17·5	11·0—9·9 10·5	11·2—9·8 10·55

TABLE IV. BENDING ANGLE IN RELATION TO THICKNESS OF WELDED SAMPLES.

Thickness [mm]	Bending angle in degrees		
	Minimum	Maximum	Average
0·8	75	100	89
1·5	55	65	61·5
2·0	55	60	57·5
2·5	50	55	53·0

Bending tests were carried out on samples from the same components cut for tensile test pieces. Results of tests are given in Table IV. Here again, heat treatment has not improved the bending resistance, and results obtained after heat treatment have not, therefore, been included in the table.

Observation of the state of samples after welding shows that welded components from which the flux has been thoroughly removed and the slag broken off, and which have been well covered with an oxide layer, have not shown any particular proneness to corrosion under laboratory conditions. Pronounced corrosion takes place when slags and fluxes have not been removed.

CONCLUSIONS

1. The carbon arc welding of magnesium alloys renders joints of high quality, provided suitable fluxes are employed.

2. The mechanical properties of welded joints are inferior to the properties of the virgin metal. Ultimate tensile strength of welded joints averages 15 kg/mm². Homogenising heat treatment of welded joints does not improve the mechanical properties.

Economic Aspects of Cutting Tool Life

By H. J. BURMESTER. (From *Werkstatt und Betrieb*, Vol. 84, No. 11, November 1951, pp. 512-516, 15 illustrations.)

The most economical cutting conditions for machining operations have already been investigated as described in refs. (1) *and* (2). *The following paper therefore deals with the economic implications of cutting tool life rather than with cutting conditions. As the subjects are closely related, the following considerations can be linked with the above mentioned papers.*

THE life of a chip-producing cutting tool is the cutting time between regrinds. The end of tool life is judged by various criteria; in machine shop parlance, the tool requires regrinding when it becomes "worn or blunt." In actual fact, tool wear commences after a few seconds of cutting and increases progressively with cutting time. There is therefore no exact instant at which the cutting tool becomes "blunt" as it is subject to continuous wear.

The following two main methods of assessing cutting tool life have been adopted in metal cutting research. The older method involves observations of sudden changes in the wear mechanism. This is the basis of the "Schlesinger criterion" utilising complete destruction of the cutting edge, the tool life criterion used by Schallbroch/Wallichs[3] using the initial breakdown of carbide tips, and of the method adopted by Leyensetter[4], where the appearance of a "glow" when turning steel with a cemented carbide tool is used as the indication of the end of useful tool life.

There are practical objections to all these methods, as in actual production they may involve partial or complete destruction of the cutting tool. Furthermore, the quality of components machined with an excessively worn cutting tool may not be acceptable.

The other method of judging the end of tool life is more acceptable. It involves the observation of various factors such as cutting resistance, width of the wear mark on the clearance face, or cratering on the top face of the tool. The progress of these is then plotted and suitable limiting values are determined. When these are reached, the tool is considered to have failed.

These methods, although more reliable and accurate, are limited in practical application, as they involve the use of various measuring equipment. The results of investigations are presented to the production engineer in the form of tables and formulae, but frequently the tool criteria used in these investigations are either impracticable or not easily measurable. The lack of information on the criterion used often makes such information virtually useless. For these reasons, the problem of cutting tool life criteria has received less attention from the production engineer than it deserves. Its economic implications are twofold. It is important that we should know whether a cutting tool will withstand, say, two hours' cutting under given conditions; the condition of the tool at the end of the two hours' cutting is equally important. The latter determines the tool costs which have to be charged against the cost of the machined components.

The following are some of the requirements which have to be fulfilled in any machining operation:

(1) The cutting forces have to be kept below certain limits to avoid risk of damage to tool, component or machine.
(2) The geometrical shape and finish of the tool have to be maintained within the required limits.
(3) The chip flow must not endanger the safety of the operator, damage the machine tool, or damage the machined surface.
(4) Production should be achieved for the lowest possible cost.

The tool requires regrinding as soon as any of these requirements are not being fulfilled. These requirements either singly or in combination constitute tool life criteria. They are all closely connected with tool wear, and it is immaterial which one of them is actually used for assessing cutting tool life. The variables which could be used include wear on the cutting edge (see Fig. 1), cutting force, component size and finish, and ship formation. Each of these variables, however, has a fairly wide permissible range, and only a cost analysis can determine the exact point at which the tool should be changed, i.e., reground.

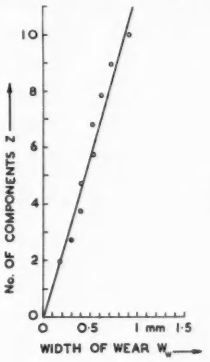


Fig. 1. **Relationship between the number of machined components and the width of the wear mark W_W.**

Tool costs C_T can be conveniently sub-divided for detailed consideration in the following manner:

(1) Tool changing costs C_{TC}. These include unclamping and clamping and re-setting of the machine, but do not include loss due to regrinding.
(2) Tool grinding costs C_{TG}, comprising labour costs of grinding, inspection and transport, but excluding the capital depreciaton due to regrinding.
(3) Tool depreciation costs C_{TD} covering capital depreciation due to regrinding, calculated as follows:

$$C_{TD} = \frac{C_{TA} - C_{TZ}}{ng + 1} \quad .. \quad .. \quad (1)$$

where C_{TA} = Original cost of new tool
C_{TZ} = Final value of worn tool
ng = Number of possible tool regrinds.

(4) Tool failure costs C_{TF} arising from complete premature failure of a tool due to chipping or breakage. They are really a part of tool depreciation costs C_{TD}, but are treated separately due to their different cause.

The suffix 1 assigned to the various symbols indicates costs connected with one component only.

Tool costs to be charged to each component depend on:—

(a) tool costs per tool life between regrinds,
(b) number of components machined between regrinds, and
(c) the relationship between the number of components between regrinds, or tool life, and tool wear.

When wear is assessed as the width of the wear mark, its variation with cutting time within the relevant limits must be known[5]. The following equation represents the general relationship between the width of the wear mark W_W and the number of components per regrind z:

$$z = K_1 W_W^{\epsilon} + K_2 \quad .. \quad .. \quad (2)$$

where K_1, x and K_2 are constants depending on cutting conditions.

K_2 is often negligible, particularly so when the exponent x is considerably less than unity; from experience, the value is known[5] to vary between 0·5 and 1.

Equation (2) can be simplified for finish turning with cemented carbide tools where the values of W_W are small:

$$z = K_1 W_W^x \quad .. \quad .. \quad .. \quad (3)$$

K_2 can thus be eliminated in cases of small wear, whereas in cases where tool wear is high, such as in roughing operations, eq. (2) becomes

$$z = K_1 W_W + K_2 \quad .. \quad .. \quad (4)$$

The value of x thus approaches unity for considerable wear such as is experienced with continuous high-speed cutting.

I. Tool Changing Costs per Component C_{TC1}.

The tool changing costs C_{TC} are approximately proportional to the time required for changing the tools and are unaffected by the extent of tool wear. The factors determining this time and, consequently, the value of C_{TC1} are the design of the machine tool, the type of cutting tool and the required component accuracy. All these factors are independent of tool wear and need not, therefore, be considered.

The tool changing costs per component C_{TC1} are dependent on tool life, as frequent tool changing will increase the costs to be charged to each component. Using eq. (2) we obtain

$$C_{TC1} = \frac{C_{TC}}{z} = \frac{C_{TC}}{K_1 W_W^x + K_2} \quad .. \quad (5)$$

The strong influence of the width of the wear mark W_W on the number of components z can be expressed by a hyperbolic law. Tool changing costs per component, therefore, decrease with an increase of permissible wear (see Fig. 2).

II. Tool Grinding Costs per Component C_{TG1}.

The determination of C_{TG} presents considerable difficulties in comparison with that of C_{TC}. As an approximation, tool grinding costs can be subdivided as follows:—

(a) Costs independent of the extent of wear. These include costs of handling, setting up, finish grinding, lapping and inspection. They can be covered by constant K_3, which will depend on the type and size of tool, available fixtures, etc.

(b) Costs dependent on the extent of tool wear. The main factor determining these costs is the volume of tool material to be ground off when rough grinding the tool. This volume can be determined from the area of the surface to be ground off (constant K_4) and from the thickness of the relevant section.

The thickness of the section to be ground off on the clearance face (see Fig. 3) depends on clearance wear and the minimum value is given by the distance between the original cutting edge and the cutting edge after wear has taken place *CED*. It is proportional to the width of the wear mark

$$CED = W_W \tan \alpha \quad .. \quad .. \quad (6)$$

The top face of the tool has similarly to be ground off to an extent depending on the vertical displacement of the cutting edge due to wear, or on the depth of crater. The thickness of this layer is approximately equal to that to be ground off the clearance face. Disregarding the volume removed during tool wear, we obtain the following expression for the minimum amount to be ground off

$$V_{G\,min} = K_4 W_W \tan \alpha \quad .. \quad .. \quad (7)$$

The actual volume ground off is, however, considerably larger than $V_{G\,min}$. For a wear mark W_W of 1 mm and a clearance angle α of 5 deg., the minimum amount to be ground off is 0·0875 mm. On a carbide-tipped tool with 9 mm available for regrinding, it should thus be possible to regrind about 100 times. In practice, the number of possible regrinds is very much smaller, partly because of the impossibility of accurately observing the wear mark during grinding. Equation (7) must, therefore, include a further constant, K_5, and becomes

$$V_G = K_4 (W_W \tan \alpha + K_5) \quad .. \quad (8)$$

Constant K_5 is surprisingly uniform for similar carbide tools worn in various ways, and it decreases almost proportionately with decreasing tool cross-section (see Fig. 4).

Regrinding costs, i.e., grinding wheel wear and labour and power costs are proportional to the volume to be ground off V_G, in so far as they are dependent on tool wear and are not included in the constant K_3. These costs, related to the volume of ground-off material, are represented by the constant K_6 in the following formula

$$C_{TG} = K_3 + K_6 V_G \quad .. \quad .. \quad (9)$$

By substituting from eq. (8) we obtain

$$C_{TG} = K_3 + K_6 K_4 (W_W \tan \alpha + K_5) \quad (10)$$

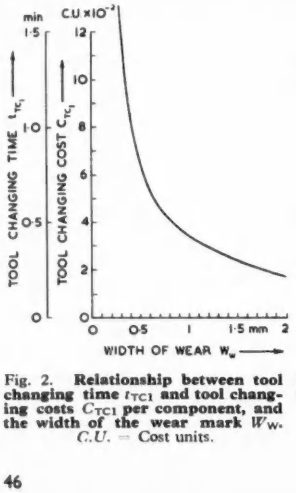


Fig. 2. **Relationship between tool changing time t_{TC1} and tool changing costs C_{TC1} per component, and the width of the wear mark W_W.** *C.U.* = Cost units.

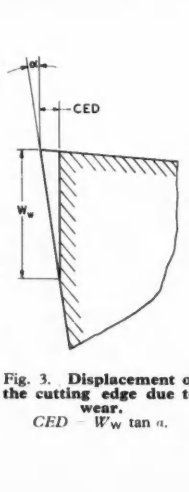


Fig. 3. **Displacement of the cutting edge due to wear.** *CED* = $W_W \tan \alpha$.

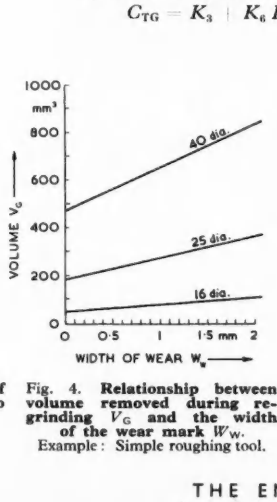


Fig. 4. **Relationship between volume removed during regrinding V_G and the width of the wear mark W_W.** Example: Simple roughing tool.

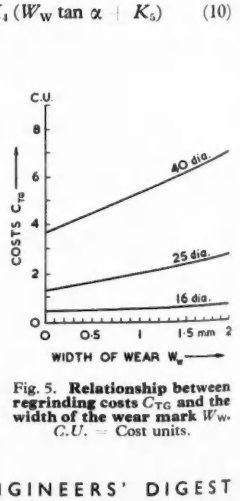


Fig. 5. **Relationship between regrinding costs C_{TG} and the width of the wear mark W_W.** *C.U.* = Cost units.

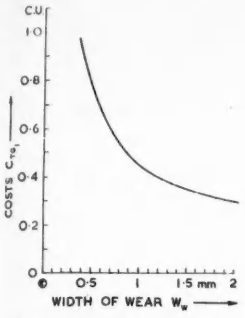


Fig. 6. **Relationship between regrinding costs per component C_{TG1} and the width of the wear mark W_W.**

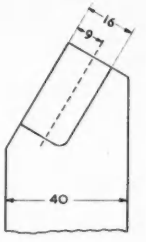


Fig. 7. **Useful tool length, L_u available for regrinding.**

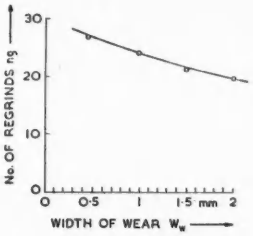


Fig. 8. **Relationship between the number of possible regrinds ng and the width of the wear mark W_W.**

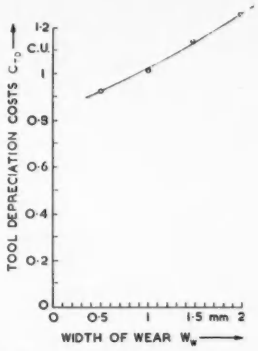


Fig. 9. **Relationship between tool depreciation costs C_{TD} and the width of the wear mark W_W.**

Example: Simple roughing tool.

Unlike C_{TC}, the magnitude of C_{TG} shows a distinct dependance on W_W (see Fig. 5), in that C_{TG} increases slightly with increasing W_W. The slight gradient is due to the fact that in eq. (10) W_W is of a low order of significance.

Equation (10) can be written as follows

$$C_{TG} = K_3 + K_4 K_5 K_6 + K_4 K_6 \tan \alpha \, W_W \quad (11)$$

It would appear that for finish turning, where the permissible tool wear is small, the third term could be neglected. W_W will, however, be significant when machining on rigid machine tools with heavy cutting tools, where considerable tool wear can be expected.

Tool grinding costs per component C_{TG1} can be obtained from eqs. (2) and (11)

$$C_{TG1} = \frac{C_{TG}}{z} = \frac{K_3 + K_4 K_5 K_6 + K_4 K_6 \tan \alpha \, W_W}{K_1 W_W^{x} + K_2} \quad (12)$$

The tool grinding costs per component C_{TG1} fall hyperbolically with increasing permissible wear, as shown in Fig. 6. In this, they resemble tool changing costs C_{TC1}.

Similar considerations apply also to cutting tools where only the rake face is reground, such as formed tools, relieved milling cutters, taps and drills. In such cases, the component tan α disappears from formula (7), which is thus simplified to

$$V_{G\,min} = K_1 W_W \quad .. \quad .. \quad (13)$$

The relationship developed above therefore remains valid.

III. Tool Depreciation Costs per Component C_{TD1}.

From eq. (1) the tool depreciation costs are inversely proportional to $ng + 1$ and the following are detailed considerations of this value.

Every cutting tool has a useful length L_u available for regrinding. The value of this dimension is not definite or clearly visible from a drawing, because in practice no tool or cutter can be fully utilised to the theoretical maximum. L_u is therefore smaller than the corresponding theoretical value.

The larger the amount of tool material removed at each regrind, the smaller will be the value of ng. In the case of a carbide-tipped turning tool, which is normally reground on the clearance face of the main cutting edge, the dimension L_u will be given by the useful width of the tip, measured at right angles to the main cutting edge, (see Fig. 7). ng can be determined as follows

$$ng = L_u/(W_W \tan \alpha + K_5) \quad .. \quad .. \quad (14)$$

An example of this is illustrated in Fig. 8.

Substituting eq. (14) in eq. (1), we obtain a relationship between tool depreciation costs and the width of the wear mark.

$$C_{TD} = \frac{C_{TA} - C_{TZ}}{ng + 1} = \frac{C_{TA} - C_{TZ}}{\dfrac{L_u}{W_W \tan \alpha + K_5} + 1}$$

or

$$C_{TD} = \frac{(C_{TA} - C_{TZ})(W_W \tan \alpha + K_5)}{L_u + W_W \tan \alpha + K_5} \quad (15)$$

Fig. 9 shows that tool depreciation costs C_{TD} rise with increasing W_W. This is due to the increasing importance, on repeated regrinding, of the ground off material volume over the displacement of the cutting edge, (constant K_5).

Division by eq. (2) gives the tool depreciation costs relative to one component

$$C_{TD1} = \frac{(C_{TA} - C_{TZ})(W_W \tan \alpha + K_5)}{(L_u + W_W \tan \alpha + K_5)(K_1 W_W^{x} + K_2)} \quad (16)$$

From this equation it is clear that values of C_{TD1} fall with increasing W_W. Whereas W_W in the enumerator appears in the first power, in the denominator its power is $(1 + x)$. Fig. 10 therefore shows a marked decrease.

IV. Tool Failure Costs per Component C_{TF1}.

This part of the cost analysis is the most difficult one, as a certain percentage of cutting tools do not reach their expected life but become useless due to failure at any time after commencement of cutting. Obvious maltreatment of the tool, such as hardness errors or stopping of the machine while cutting, are not considered under this category; even without these obvious causes, a certain number of apparently inexplicable tool failures does occur.

The incidence of these failures is difficult to determine and cannot be judged by observation of a limited number of tools.

For the purpose of this analysis, the incidence of breakage for two different tool shapes was determined, and Fig. 11 shows the percentage of tip failures as a function of the width of wear W_W. The resulting curves are surprisingly smooth and regular. The permissible wear is thus at least one of the factors governing the incidence of failure and it can be assumed that it is probably the main factor in heavy-duty rough turning. These failures decrease the total effective tool life when

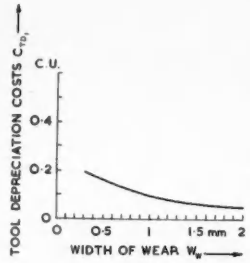


Fig. 10

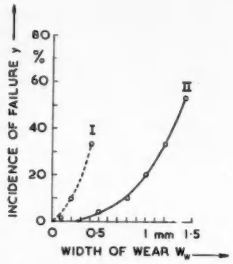


Fig. 11

Fig. 10. **Relationship between tool depreciation costs per component C_{TD1} and the width of the wear mark W_W.**

Fig. 11. **Relationship between the incidence of failure in per cent of two differently ground tools, and the width of the wear mark W_W.**

considering a large number of tools, so that the resultant value will be less than ng as shown in Fig. 8.

A mathematical solution is possible with the following considerations:—Assuming no breakages, each tool can machine $Z_1 = (ng + 1)\,z$ components, whereas the number of components machined with T tools is

$$TZ_1 = T\,(ng + 1)\,z \qquad .. \qquad .. \qquad (17)$$

Premature tool failures will, however, reduce the number of components machined with T tools and, conversely, a larger number of tools would be required for machining Z components.

Assuming no failures, the number of tools T required for machining Z components will be

$$T = \frac{Z}{Z_1} = \frac{Z}{(ng + 1)\,z}$$

The problem becomes rather more involved if y per cent of the tools fail for each regrind. In the first regrind, T tools are reduced by $y/100 \times T$ due to failures and the number available for further regrinding is

$$T_1 = T\,(1 - y/100) \qquad .. \qquad .. \qquad (18)$$

After $(ng + 1)$ regrinds, the number of remaining tools is only

$$T_{ng-1} = T\,(1 - y/100)^{ng+1} \qquad .. \qquad (19)$$

At this stage, no further tools can be reground, as even those tools that did not fail completely have become completely useless.

The above considerations are correct under the following two assumptions:

(a) Each failure represents a total loss.
(b) The incidence of failure remains almost constant throughout the useful life of the tool.

The number of components machined with tools not subjected to failure is

$$Z_N = (T_I + T_{II} + T_{III} + \ldots\ldots Tng + 1)\,z$$

Using eqs. (18) and (19) we obtain

$$Z_N = 1 - y/100\,\frac{1 - (1 - y/100)^{ng+1}}{1 - (1 - y/100)}\,Tz \quad (20)$$

The determination of the number of components machined with tools that do fail is difficult, as some tools may have failed early, whereas others may have failed shortly before the end of their normal useful life. Some guidance can, however, be obtained from the curves in Fig. 11.

To simplify the problem, we can assume that the probable life of a tool which has prematurely failed corresponds to $0{\cdot}8z$ machined components. The probable error of this assumption is acceptable when we compare the curve c ($W_{W\ probable} = 0{\cdot}8\,z$) with the exact curves a and b in Fig. 12.

The number of components machined with tools subjected to failure Z_F can now be calculated with a certain degree of accuracy.

$$Z_F = (y/100)\,\frac{1 - (y/100)^{ng+1}}{1 - (y/100)} \times 0{\cdot}8\,T\,z \qquad (21)$$

The total number of components machined with T tools can now be arrived at by adding eqs. (20) and (21)

$$Z_{N+F} = Z_N + Z_F \qquad .. \qquad .. \qquad (22)$$

From the relationship of Z to Z_{N+F} it is clear that the tool failure costs C_{TF} will be proportional thereto. It also follows that C_{TF} will influence the original cost of the total tooling and is, therefore, closely linked with the value $(C_{TA} - C_{TZ})$. C_{TF} will be higher with the more expensive tools, assuming equal incidence of failures, and lower with the less expensive tools.

C_{TF} can therefore be given as

$$C_{TF} = (C_{TA} - C_{TZ})\,\frac{Z}{Z_{N+F}} - (C_{TA} - C_{TZ}) \qquad .. \qquad (23)$$

By dividing eq. (23) by eq. (2) we obtain C_{TF1} (illustrated in Fig. 13) and it is clear that C_{TF1} rises sharply with increasing W_W, with y increasing similarly. This is contrary to all other costs and enables clear conclusions to be drawn.

TOTAL TOOL COSTS PER COMPONENT C_{T1}.

C_{T1} comprises a combination of the four costs developed above

$$C_{T1} = C_{TC1} + C_{TG1} + C_{TD1} + C_{TF1} \qquad .. \qquad (24)$$

From the above considerations it follows that:—

(a) When C_{TF1} is almost zero, i.e., when there is little likelihood of tool failure, the value C_{T1} will be smaller the greater the permissible wear, and
(b) where C_{TF1} is zero, there will be an optimum cost, determined by plotting C_{T1} against W_W.

Fig. 14 illustrates the first case where $C_{TF1} = 0$. The sum of the three costs falls hyperbolically with increasing W_W. Fig. 15 illustrates the second case where $C_{TF1} \neq 0$. The three costs C_{TC1}, C_{TG1} and C_{TD1} still fall with increasing W_W, whereas tool failure costs C_{TF1} determine a clear minumum point of the total costs.

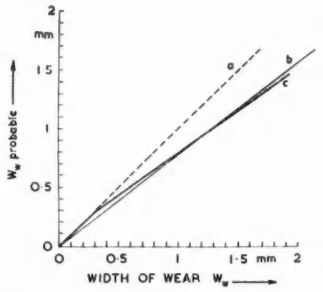


Fig. 12

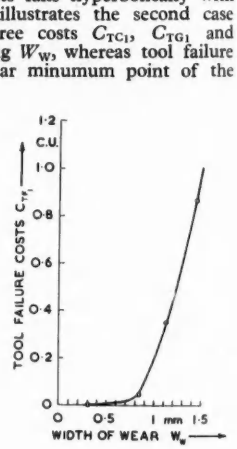


Fig. 13

Fig. 12. **Probable tool life of a single point tool, under given test conditions.**

(a) $W_{W\ probable} = W_W$, i.e. no failure.
(b) Actual observed tool life.
(c) Approximate curve, assuming $z_{probable} = 0{\cdot}8\,z$.

Fig. 13. **Relationship between tool failure costs per component C_{TF1} and the width of the wear mark W_W.**

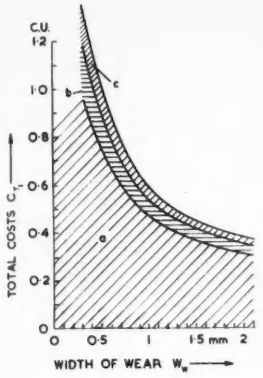


Fig. 14

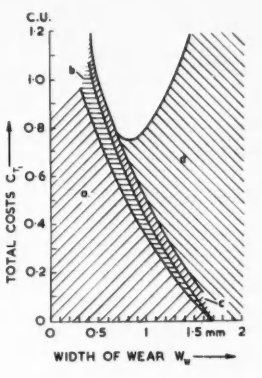


Fig. 15

Fig. 14. **Total tool costs per component C_{T1}, assuming no failures.**

(a) Tool grinding costs per component C_{TG1}.
(b) Tool depreciation costs per component C_{TD1}.
(c) Tool changing costs per component C_{TC1}.

Fig. 15. **Total tool costs per component C_{T1}, taking failures into consideration.**

(a) Tool grinding costs per component C_{TG1}.
(b) Tool depreciation costs per component C_{TD1}.
(c) Tool failure costs per component C_{TF1}.

Example: Clearly defined minimum at $W_W = 0{\cdot}9$ mm, i.e., after approx. 10 components according to Fig. 1.

CONCLUSIONS.

The object of this cost analysis was the determination of the most economic tool life, and the following recommendations can now be made:—

Providing there is no danger of tool failures, the tools should be utilised to the maximum permissible degree of wear. If, for example, the quality of the surface finish is critical, the tools should not be reground while the surface finish obtained is still acceptable. In this case, therefore, frequent regrinding is uneconomical.

If, however, considerable wear is permissible and surface finish quality is only a secondary consideration, such as in rough turning or milling, the tool failure costs C_{TF1} become the dominant factor and determine the economic optimum. This case arises, for example, with simple cemented carbide tools, when failures reach approximately the 5 per cent level.

In general, it can be said that prevention of tool failure will become more important with higher original tool costs. The economic tool life of a cemented carbide-tipped multi-tooth cutter will, for instance, be in the region of smaller wear than that of a single tooth cutter. This is due to the higher original cost of the former and a consequent steep gradient of the C_{TF} curve.

No specific recommendations can be made in this field due to the vast number and variety of metal cutting tools. The cost analysis developed above should therefore be considered as a general method for guidance.

SUMMARY

The economy of a machining operation is determined not only by the operating conditions, but also by the choice of a suitable "tool life."

The above considerations have shown that there are four costs related to tool wear. Three of them fall with increasing permissible tool wear, whereas the fourth increases the costs, as it represents costs for tools which have prematurely failed.

It can therefore be concluded that for finishing operations and for machining on relatively weak machines, the economic optimum tool life will be associated with high permissible tool wear.

Heavy-duty simple roughing tools should also be fully utilised up to a 3-5 per cent failure level, but expensive tools or cutters should only be utilised to a point where there is negligible risk of tool failure. The criteria for an economic tool life are therefore tool costs for roughing, and component requirements for finishing operations.

It must be appreciated that a full cost analysis can be made only in a few cases. The production engineer should, however, be familiar with the basis of such calculations to enable him to arrive at reasoned estimates, and to give him a deeper understanding of the interdependence of various cost items and their origin.

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CONSERVATION OF STRATEGIC MATERIALS IN GERMANY

(*Concluded from page* 38)

are not equivalent to nickel. Provided corrosion conditions are not too severe, savings in nickel can be obtained by using stainless irons or steels with a greatly reduced nickel content. The processing of these substitute steels is generally more difficult and they also appear to be less suitable for decorative purposes.

Conservation and replacement of non-ferrous metals. Important consumer savings in light metals can be obtained by painstaking sorting with modern electric conductivity equipment. The best overall results are generally obtained by scrap refining. Pure magnesium can be distilled from "elektron" scrap (magnesium-aluminium alloy). High-quality zinc alloys can be obtained from zinc scrap. Zinc can be saved to some extent by substituting light metals for zinc, including brass. Substitutes for most zinc paints are available. Economy in galvanized steel parts is possible by using thinner electroplated coatings or even paints and lacquers. Tin and lead can be replaced by varnishes, light metal alloys, or plastics. Tin is essential, however, in bearing metals and solders.

Copper is replaceable by aluminium for high-tension power lines and most electrical lines down to the house connection. However, the replacement of copper for electric wires inside houses, including telephone and telegraph wires, is not recommended. Moreover, aluminium or zinc cannot replace copper in important armatures.

The consumer is accustomed by fashion to demand a luxurious surface appearance for his accessories on motor-cars, refrigerators, etc. Valuable materials could be saved, however, by using chemically polished and electrolytically brightened 99·998 per cent pure aluminium. This is obtained by three-layer electrolysis from ordinary aluminium scrap and hardened by the addition of certain alloys.

New Two-Stroke Cycle Vee-Type Diesel Engines

By L. RICHTER. (From *Zeitschrift des Österreichischen Ingenieur- und Architekten-Vereines*, Vol. 96, No. 23/24, December 3, 1951, pp. 197-201, 10 illustrations.)

Two new Vee-type two-stroke cycle diesel engines, designated 4ZT and 6ZT, are now being produced by the Gräf & Stift Motor Car Co., of Vienna. These four- and six-cylinder engines were designed in co-operation with the engine makers by the engineering offices of Professor H. List, of Graz.

Sectional drawings of these water-cooled engines are shown in Figs. 1 and 2. Both are compact designs, with short overall lengths, in spite of the high power developed. The compact designs are specially evolved for lorries and buses, and are also of advantage as regards cost of crankshaft and torsional vibration characteristics.

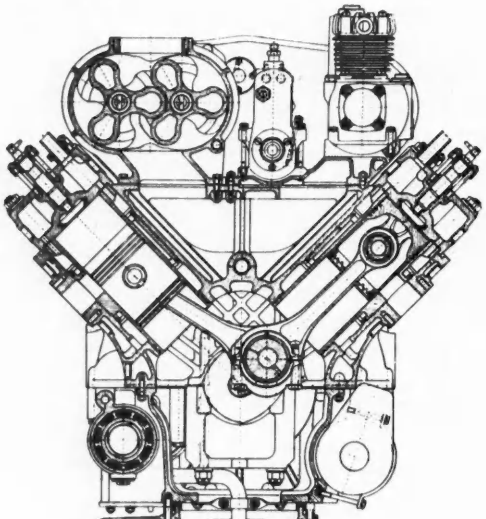

Fig. 1. **Sectional view of the 4-cylinder engine 4ZT.**

Loop scavenging is used, with three exhaust ports and five inlet ports on each side of these per cylinder, all ports being controlled by the piston. The scavenge air is supplied by a three-lobed Roots-type blower running practically noiselessly at 1½ times the engine speed. No special timing gear being required with port scavenging, the cylinder head is a simple design containing only the injection nozzle and the blow-off valves for braking. The thermal stresses in the cylinder head are thus symmetrically distributed with respect to the cylinder axis and result in low thermal loads. The cylinder liner is a high-grade centrifugal casting flanged on to the cylinder head and can move freely in the axial direction.

The gas forces are transmitted by the cylinder head through tie bolts to the crankcase. The Roots blower is considered to be superior to turbo-blowers owing to the considerably reduced effect of back pressure on the air charge.

The engines have a 90-degree Vee arrangement with a scavenge air chamber between the banks of cylinders, the design being such as to ensure good accessibility to the scavenge blower, the fuel pump, the dynamo, and the air compressor used for braking. The engines have a high brake mean effective pressure and a very uniform torque.

ENGINE DATA

Type: 4-, and 6-cylinder 2-stroke cycle engines, 90-degree Vee arrangement of cylinders. Bore: 120 mm. Stroke: 140 mm. Cylinder capacity: 1·583 litres. (In the following lines, the first figures refer to the 4-cylinder version, the corresponding values for the 6-cylinder engine being given in brackets.) Total cyclinder capacity 6·332 litres (9·498). Dry weight of engine including auxiliaries 540 kg (695). Peak short-

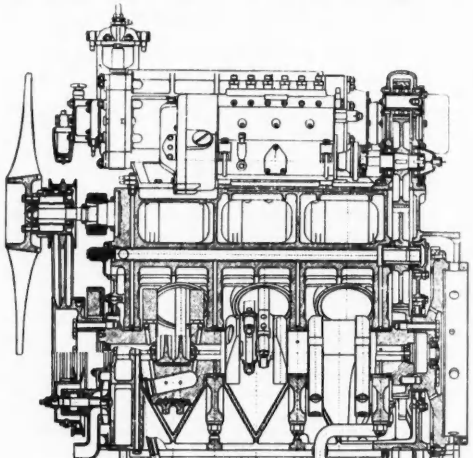

Fig. 2. **Sectional view of the 6-cylinder engine 6ZT.**

time power 125 (185) bhp, at 2000 rpm. Weight/power ratio 4·3 (3·8) kg/(metric)hp. Maximum speed 2100 rpm. Tick-over speed 450 rpm. Maximum torque 48-50 mkg (72-78) at 1100 and 1600 rpm, respectively. Minimum fuel consumption 168 (165) grams/bhp-hr. Direct injection with multi-hole nozzle, flat piston crown, combustion chamber in cylinder head.

Light-metal crankcase. Two (three)-throw crankshaft, with 3(4) journal bearings. Journal and big-end bearings of lead bronze. Side-by-side connecting rods. Balance weights on webs of first and last crankpins. Wet cylinder liners with radiused ports. Gearbox at flywheel end driving the dynamo, the fuel pump, oil pump, Roots blower and brake compressor. Loop scavenging; Roots blower gear-driven, transmission ratio 1 : 1·51, rotor speed 3020 rpm at rated engine speed. Air consumption 1·4 times the swept volume.

Fuel pump size B, of Robert Bosch A.G., or Friedmann & Maier; two-stroke cam, 8 mm plunger, without injection adjustment; idling and maximum speed governor. Multiple-hole nozzles, size S, of Friedmann & Maier, or C.A.V. Ltd. Gear-driven lubricating oil pump; heat exchanger with water cooling; magnet-bar filter; oil sump of 16 (24) litres capacity; central oil distributing system for the main bearings. Cooling water circulating system with a flow of 180 litres per minute at rated engine speed; water,

pump and fan driven by Vee-belt. Starter 6 hp 12 volts. Dynamo 12 volts, 300 watts. Starting aid: For outside temperatures below 0° C, automatic pre-heating of the scavenge air in the air chamber. Engine braking: Optional auxiliary equipment, for operation of blow-off valves in the cylinder heads.

Test Results for the 4ZT Engine

While the six-cylinder engine was undergoing an extensive series of road tests, the four-cylinder model 4ZT was investigated by Professor S. J. Davies, of King's College, London University*.

Tests with a fuel having a lower calorific value of 9940 kcal/kg gave as a lowest value for fuel consumption 164 grams per (metric) bhp-hr, or 1632 kcal/bhp-hr at 1250 rpm and 5·06 kg/cm² bmep, which corresponds to an optimum efficiency of 38·7 per cent. The mechanical losses were determined by plotting the fuel consumption against bmep-values and extrapolating the approximately straight lines to negative bmep-values (Romber-Willans lines), with different lines for 1000, 1250, 1500, 1750 and 2000 rpm. The frictional mep-values thus obtained are lower than those determined with the engine driven by external means. Owing to the limit control of the quantity of fuel injected, the highest bmep-values are situated well below the smoking limit so that a clear exhaust is obtained even after a long period of operation.

*See also: *Engineering*, Vol. 172, 1951, pp. 1-5.

NORWAY

Theory of Turbulent Flow and its Application to Flow Resistance and Heat Transfer

By D. Johnson. (From *Teknisk Ukeblad*, Vol. 98, No. 34, September 20, 1951, pp. 675-681, 8 illustrations.)

A new formula is presented for the velocity distribution in the transition layer. This expression is then used for the determination of the flow resistance coefficient. The resultant equation gives close agreement with both Blasius' and Nikuradse's relations and is valid for Reynolds numbers between 10^4 and 10^6. For heat transfer calculations, a new expression is obtained for Nusselt's number, which is also valid for Prandtl numbers below 10.

The calculation of heat transfer in turbulent flow is based on the relation between momentum transfer and heat transfer. Accurate knowledge is, therefore, required of the velocities and thicknesses of the various layers of fluid involved.

On the basis of Prandtl's boundary layer theory, the flow (see Fig. 1) can be subdivided into three layers:— (1) a boundary layer y_{lam} with laminar flow conditions, (2) a transition layer $y_{tr} - y_{lam}$ in which primary eddies occur, and (3) a turbulent core $r - y_{tr}$ (r = tube radius) in which the turbulence is fully developed.

When the fluid has a very high Prandtl number $Pr = \nu/a$ (where ν = kinematic viscosity, and a = thermal diffusivity), there is a steep temperature gradient near the wall of the tube. In the extreme case, the entire temperature drop is situated in the laminar region (curve B of Fig. 1). The heat transfer coefficient is then

$$\alpha = \lambda_{lam}/y_{lam} \text{ when } Pr \to \infty \quad .. \quad (1)$$

λ_{lam} being the average thermal conductivity in the laminar layer.

For fluids with a low Prandtl number, the temperature drop θ_{lam} in the laminar region (curve C of Fig. 1) is much lower than in the previous case. There is a considerable temperature drop $\theta_{tr} - \theta_{lam}$ in the transition zone $y_{tr} - y_{lam}$.

In order to obtain a general velocity diagram, it is convenient to introduce non-dimensional quantities, viz.:—

the velocity ratio $u^+ = u/u^*$

and the relative wall distance $y^+ = yu^*/\nu$.

The diagram is then obtained by plotting u^+ for different values of y^+ (see Fig. 2). In the above expressions: u = velocity at any point in the tube cross-section; $u^* = \sqrt{\tau_w/\rho}$ = "shear stress velocity"; τ_w = shear stress at the wall; ρ = mass density of fluid; and ν = kinematic viscosity of fluid.

For the velocity variation in the turbulent core, Prandtl evolved the following formula in 1933:—

$$u^+ = 2{\cdot}5 \log_e y^+ + 5{\cdot}5 \quad .. \quad .. \quad (2)$$

For the linear velocity variation in the laminar layer, the equation is simply

$$u^+ = y^+ \quad .. \quad .. \quad .. \quad (3)$$

The two corresponding curves are shown in Fig. 2. The curve representing velocity in the transition layer should be situated somewhere between these two curves.

The layer thickness is an important subject. Heat transfer measurements indicate that $y^+_{lam} = 1{\cdot}5$. The transition layer has values y^+_{tr} between 40 and 50. In this analysis, the lower of these two values will be used. Both y^+_{lam} and y^+_{tr} are indicated by dotted lines in Fig. 2.

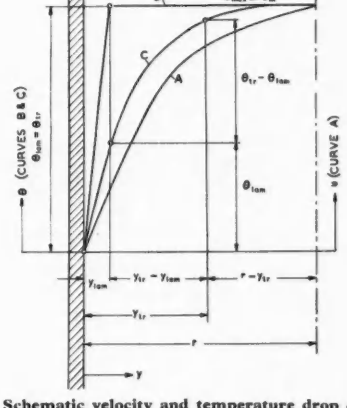

Fig. 1. **Schematic velocity and temperature drop diagram for flow in a tube of radius r.**
y = wall distance; u = velocity; θ = temperature.

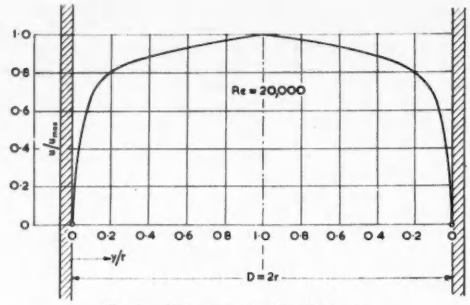

Fig. 2. **General velocity diagram.**
y^+ = reduced wall distance ; u^+ = velocity ratio.

Accurate flow velocity measurements in tubes show small but significant deviations from Fig. 2, in which the curves have a certain slope relatively to the centre line of the tube. Fig. 3 shows a complete diagram calculated from Fig. 2, with the relative velocity u/u_{max} plotted against the wall distance ratio y/r, for a Reynolds number of 20,000. The real diagram should have a zero tangent at the tube centre. However, closer to the wall, the diagram is sufficiently correct for both heat transfer and pressure determinations.

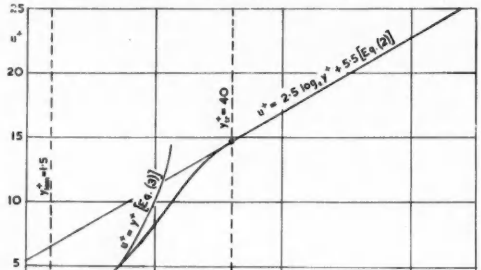

Fig. 3. **Complete diagram for the velocity variation u/u_{max} for a Reynolds number of 20,000, based on Fig. 2.**

In 1910, Prandtl assumed that the turbulent core borders directly upon the laminar boundary layer, the change-over occurring at a shearing point A, as shown in diagram I of Fig. 4. He assigned to the laminar layer an equivalent thickness $y^+_{lam} = 11{\cdot}5$.

Kármán was the first to use the general velocity diagram for heat transfer calculations. In 1939, he proposed $y^+_{lam} = 5$ and $y^+_{tr} = 30$. The velocity variation in the transition layer was represented as indicated in diagram II of Fig. 4. A further important contribution was made by Reichardt, who determined the values for relative thickness as $y^+_{lam} = 2{\cdot}2$ and $y^+_{tr} = 40$ and also gave a logarithmic expression for the velocity in the transition layer (diagram III in Fig. 4).

An investigation made in 1948 by the author of this article gave the values $y^+_{lam} = 1{\cdot}5$ and $y^+_{tr} = 50$. It also led to the development of the following formula for velocity in the transition layer :—

$$u^+ = 1{\cdot}5 + 10{\cdot}05 \tan^{-1} [0{\cdot}0995 (y^+ - 1{\cdot}5)] \quad .. \quad (4)$$

(see diagram IV of Fig. 4).

The effects of these various assumptions on the heat transfer calculations are compared in Fig. 5. The thick line marked D and B is an empirical curve, according to Dittus and Boelter. The curves are calculated for a Reynolds number of 25,000 and represent the

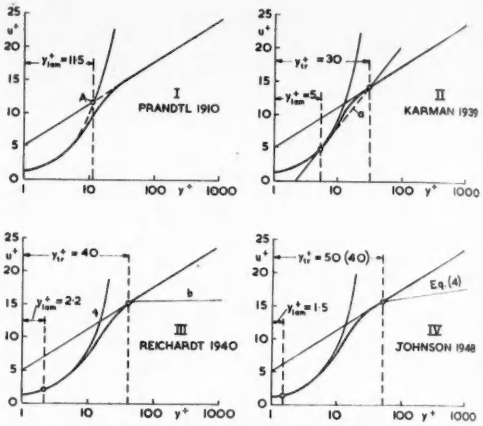

Fig. 4. **Comparison of various curves and coefficients used for the general velocity diagram.**

variation of the heat transfer number C_h as a function of the Prandtl number, C_h being determined from

$$C_h = q_w/(\gamma_m c_m u_m \theta_m) = \alpha/(\gamma_m c_m u_m) \quad .. \quad (5)$$

where q_w = radial specific heat flow near the tube wall, γ_m = average specific gravity of fluid, c_m = mean specific heat of fluid, u_m = mean flow velocity, and θ_m = mean temperature difference.

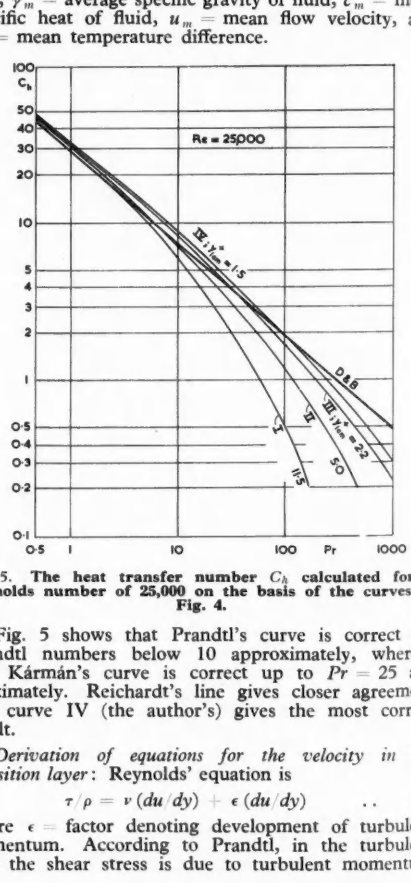

Fig. 5. **The heat transfer number C_h calculated for a Reynolds number of 25,000 on the basis of the curves of Fig. 4.**

Fig. 5 shows that Prandtl's curve is correct for Prandtl numbers below 10 approximately, whereas von Kármán's curve is correct up to $Pr = 25$ approximately. Reichardt's line gives closer agreement and curve IV (the author's) gives the most correct result.

Derivation of equations for the velocity in the transition layer: Reynolds' equation is

$$\tau/\rho = \nu\,(du/dy) + \epsilon\,(du/dy) \quad .. \quad (6)$$

where ϵ = factor denoting development of turbulent momentum. According to Prandtl, in the turbulent core the shear stress is due to turbulent momentum

and can be represented by

$$\tau/\rho = \epsilon\,(du/dy) = (ky)^2\,(du/dy)^2 \quad .. \quad (7)$$

For the transition layer, the author assumes that the shear stress is the result of the following terms:—

$$\tau = \tau_\mu + \tau_t + \tau_v \quad .. \quad .. \quad .. \quad (8)$$

where $\tau_\mu = \mu\,du/dy$ = viscous shear stress; τ_t = shear stress due to turbulence momentum; and τ_v = shear stress due to boundary eddies.

In considering the tube cross-section, it will now be assumed that a portion $1 - \varphi$ of each unit area in the section is filled with boundary eddies, which cause turbulent motion by momentum transfer. It is reasonable to assume that the shearing force τ_v' of the boundary eddies is proportional to the local value of the shearing force τ_μ of the turbulence, per unit area, according to Prandtl's definition, so that

$$\tau_v' = K\,\tau_\mu \quad .. \quad .. \quad .. \quad .. \quad (9)$$

where K is a proportionality constant, and

$$\tau_v = (1 - \varphi)\,K\,\tau_\mu \quad .. \quad .. \quad .. \quad (9')$$

No other assumptions are necessary regarding, for instance, the magnitude, shape or distribution of the eddies. The remaining portion φ per unit area of the section is filled with particles in turbulent motion. For these it can be assumed that the local shear force of turbulent momentum τ_t' is equal to Prandtl's shearing force of turbulence τ_μ per unit area. Therefore,

$$\tau_t' = \tau_\mu \quad \text{or} \quad \tau_t = \varphi\,\tau_\mu \quad .. \quad .. \quad (10)$$

and eq. (8) can be rewritten as

$$\tau = \tau_\mu + \varphi\,\tau_\mu + (1 - \varphi)\,K\,\tau_\mu$$

or $\tau/\rho = \nu\,(du/dy) + [\varphi + (1 - \varphi)\,K]\,(ky)^2\,(du/dy)^2$

so that $\tau/\rho = \nu\,(du/dy) + \Psi y^2\,(du/dy)^2 \quad .. \quad (11)$

where $\Psi = [\varphi + (1 - \varphi)\,K]\,k^2 \quad .. \quad .. \quad (11')$

and $k = 0{\cdot}4 = 1/2{\cdot}5$ (see eq. (4)).

Equation (11) can be expressed in a non-dimensional form:—

$$[1 + \Psi(y^+)^2\,(du^+/dy^+)]\,(du^+/dy^+) = 1 \quad (12)$$

At the boundary of the laminar layer, $du^+/dy^+ \simeq 1$, and turbulent momentum is very low, so that $\varphi \simeq 0$. With these conditions, we obtain

$$\Psi\,du^+/dy^+ \simeq C\ (= \text{constant}) \quad .. \quad (13)$$

and eq. (12) is reduced to

$$[1 + C\,(y^+)^2]\,(du^+/dy^+) = 1 \quad .. \quad (14)$$

Integration between the limits 0 and y^+ gives

$$u^+ = (1/\sqrt{C})\,\tan^{-1}\,(\sqrt{C}\,y^+)$$

which is also valid for the laminar layer thickness $y^+_{\text{lam}} = 0$. For non-zero values of y^+_{lam}, the function becomes

$$u^+ = y^+_{\text{lam}} + (1/\sqrt{C})\,\tan^{-1}\,[\sqrt{C}\,(y^+ - y^+_{\text{lam}})].$$

This equation can be adapted to the empirical velocity curve in the transition layer by putting $C = 0{\cdot}01$ and $y^+_{\text{lam}} = 1{\cdot}5$, the latter value having been obtained from previous experiments by the author. The result is

$$u^+ = 1{\cdot}5 + 10\ \text{arc tan}\,[0{\cdot}1\,(y^+ - y^+_{\text{lam}})] \quad .. \quad (15)$$

This equation is identical with eq. (4), apart from an insignificant difference in the value of the constant. The curve determined by eq. (15) is tangent to the logarithmic velocity curve of the turbulent layer, represented by eq. (2), at the point $(y^+_{\text{tr}};\ u^+_{\text{tr}}) = (40;\ 14{\cdot}7)$.

Equation (15) gives very good agreement with experimental values not only in the vicinity of the laminar layer but also in other parts of the transition layer.

Combining eqs. (11′) and (13) we obtain

$$\Psi\,du^+/dy^+ = [\varphi + (1 - \varphi)\,K]\,k^2\,du^+/dy^+ = C = 0{\cdot}01 \quad (16)$$

This approximation must be valid for the entire transition layer. At the laminar layer boundary, $y^+ = y^+_{\text{lam}}$ and $\varphi = 0$ (the boundary portion is filled with primary eddies). Moreover, in this instance, $du^+/dy^+ = 1$. Equation (16) then gives: $K = C/k^2 = 0{\cdot}0625$. At the boundary of the transition layer, $y^+ = y^+_{\text{tr}}$ and $\varphi = 1$ (the entire section is filled with particles in Prandtl-type turbulent motion) and eq. (16) then gives: $(du^+/dy^+)_{\text{tr}} = C/k^2 = 0{\cdot}0625$. With the $\tan^{-1}$-function of eq. (15), this corresponds to a value $y^+_{\text{tr}} = 40{\cdot}2$ which is much closer to the previously determined limit $y^+_{\text{tr}} = 40$.

The variations of φ and $1 - \varphi$ are determined by combining eqs. (15) and (16) and introducing the values for k and K obtained above. Then

$$\varphi = (y^+ - 1{\cdot}5)^2/1500 \quad .. \quad .. \quad (17)$$

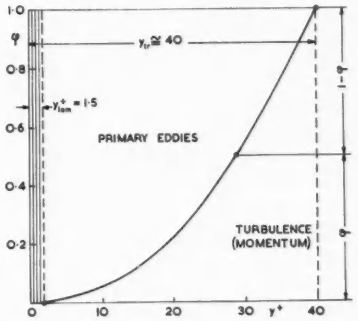

Fig. 6. Variation of the turbulence contribution φ and of the contribution of the primary eddies $1 - \varphi$ in the transition region. (See eq. (17)).

The resulting distribution is shown in Fig. 6. The contribution $1 - \varphi$ of the primary eddies decreases from unity to zero in the interval y^+_{lam} to y^+_{tr}, while the relative contribution φ of the particles with turbulent momentum increases from zero to unity in the same interval.

Mean flow velocity and flow resistance coefficient: The velocity function in the three layers is now determined by eqs. (2), (3) and (15). On this basis, the mean flow velocity u_m^+ for the cross-section can be calculated in order to determine the flow resistance coefficient ζ for smooth tubes.

The drop in pressure per metre of tube length is given by

$$\Delta p = (\zeta/d)\,\rho\,(u_m{}^2/2) = 4\,\tau_w/d \quad .. \quad (18)$$

so that $\zeta = 8/[u_m{}^2\,\rho/\tau_w] = 8/(u_m{}^+)^2 \quad .. \quad (18')$

The value of $u_m{}^+$ for the various layers is determined as

$$u_m{}^+ = \frac{2}{(r^+)^2}\left[\int_0^{1{\cdot}5} f\,dy^+ + \int_{1{\cdot}5}^{40} g\,dy^+ + \int_{40}^{r^+} h\,dy^+\right]$$

where $f = y^+\,(r^+ - y^+)$;

$g = [1{\cdot}5 + 10\,\tan^{-1}\,0{\cdot}1\,(y^+ - 1{\cdot}5)]\,(r^+ - y^+)$;

and $h = [2{\cdot}5\,\log_e y^+ + 5{\cdot}5]\,(r^+ - y^+)$.

The integration gives

$$u_m{}^+ = 2\,[1{\cdot}25\,\log_e r^+ + 0{\cdot}88 - (60{\cdot}5/r^+) + 617/(r^+)^2].$$

The last term is very small even for low Reynolds numbers, so that a good approximation is obtained with

$$u_m{}^+ \simeq 2\,[1{\cdot}25\,\log_e r^+ + 0{\cdot}88 - (60{\cdot}5/r^+)] \quad .. \quad (19)$$

The flow resistance ζ is to be expressed as a function of the Reynolds number. The wall distance ratio can be re-written as follows:

$$r^+ = u^*r/\nu = (u_m\,r/\nu)\,(u^*/u_m) = Re/(2\,u_m{}^+).$$

This gives the relation

$$Re = 2\,u_m{}^+ r^+ \quad .. \quad .. \quad (20)$$

Equations (19) and (20) then give

$$u_m{}^+ = 2\,[1{\cdot}25 \log_e (Re/2u_m{}^+) + 0{\cdot}88 - (121 u_m{}^+/Re)] \quad (21)$$

This can be re-written as

$$[(242/Re) + 1]\,u_m{}^+ + 2{\cdot}5 \log_e u_m{}^+ = 2{\cdot}5 \log_e Re \quad (21')$$

The value of $u_m{}^+$ varies between 12 and 28 for the range of Reynolds numbers from 2400 to 2×10^6. As a mean value, we can take $u_m{}^+ \simeq 20$.

We shall now make use of the relation

$$u_m/20 \sim 1 + \log_e(u_m{}^+/20) + (1/2!)\,[\log_e(u_m{}^+/20)]^2 + \ldots$$

The terms of higher order are very small. Combining this relation with eq. (21′) and substituting for $u_m{}^+$ the expression with ζ of eq. (18′), we obtain

$$\zeta = 0{\cdot}148 \exp\left\{a - 2\sqrt{b}\right\} \quad .. \quad .. \quad (22)$$

where $a = Re/[4\,(Re + 242)]$ and $b = [1 + (a/2)]^2 - 2 + a\,(\log_e Re - 3)$. For Reynolds numbers above 50,000, the value of a is approximately equal to 0·25. With this value, eq. (22) becomes

$$\zeta = 0{\cdot}19 \exp\left\{-\sqrt{\log_e (266\,Re/10^5)}\right\} \quad .. \quad (23)$$

This equation can be compared with the formulae of Blasius: $\zeta = 0{\cdot}316/Re^{1/4}$ (for $2400 \leq Re \leq 10^5$), and Nikuradse: $= 0{\cdot}0032 + 0{\cdot}221/Re^{0{\cdot}237}$ (for $Re \geq 10^5$). The following table gives a comparison of values calculated by these three equations.

Re	Eq. (23)	Blasius	Nikuradse
	Flow resistance coefficient ζ		
3,000	0·0449	0·0428	—
10,000	0·0311	0·0316	—
30,000	0·0238	0·0240	—
100,000	0·0178	0·0178	0·0178
300,000	0·0143	—	0·0144
1,000,000	0·0115	—	0·0117

These results are also shown graphically in Fig. 7. The values based on eq. (23) are indicated by circles. It is thus apparent that eq. (23) gives results of considerable accuracy both in the Blasius and the Nikuradse range.

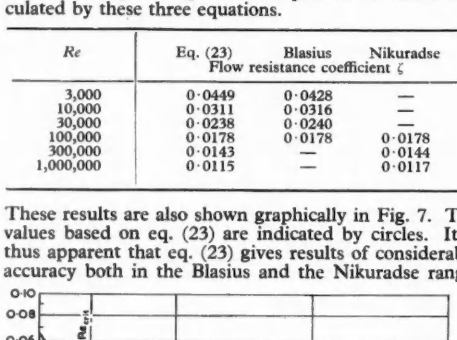

Fig. 7. **Flow resistance coefficient for smooth tubes.**
The values denoted by circles and calculated by means of eq. (23) agree with Blasius' curve for Reynolds numbers up to 10^5 and with Nikuradse's curve for higher Reynolds numbers.

Calculation of heat transfer in turbulent flow: The new flow-resistance function ζ determined by eq. (23) will now be used for the calculation of heat transfer with turbulent flow conditions in a tube.

Prandtl's original formula, published in 1910, is

$$Nu = (\zeta/8)\,Pé/[1 + \varphi\,(Pr - 1)]$$

where $Nu = \alpha\,d/\lambda$ = Nusselt's number (λ = thermal conductivity of fluid); and $Pé = u_m\,d/a$ = Péclet's number (a = thermal diffusivity of fluid). The value of φ is determined empirically. According to Hofmann, $\varphi = 1{\cdot}5/(Re^{0{\cdot}125} \times Pr^{0{\cdot}167})$. In general, Blasius' expression for ζ is used, and this gives

$$Nu = 0{\cdot}0395\,Pé\,Re^{-0{\cdot}25}/\Lambda \quad .. \quad .. \quad (24)$$

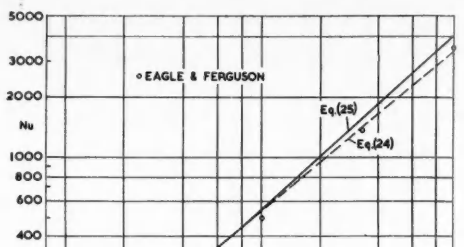

Fig. 8. **Calculated heat transfer coefficient for water with a Prandtl number** $Pr = 5$.
The dotted line represents the Prandtl-Hofmann curve (eq. (24)); the values obtained by Eagle and Ferguson and extrapolated beyond 3×10^5 are indicated by circles; and the solid line represents the curve corresponding to eq. (25).

where $\Lambda = 1 + 1{\cdot}5\,Re^{-0{\cdot}125}\,Pr^{-0{\cdot}167}\,(Pr - 1)$

Substituting for ζ the value from eq. (23) we obtain

$$Nu = 0{\cdot}0238 \exp\left\{-\sqrt{\log_e (266\,Re/10)^5}\right\} \times Pé/\Lambda \quad (25)$$

Fig. 8 shows curves of Nusselt numbers determined from eqs. (24) and (25), for a Prandtl number of 5 (which applies, for instance, for water at about 30° C). Eq. (24) is valid over the same range as Blasius' formula, that is, up to approximately $Re = 10^5$. Eq. (25) can be assumed to be valid for much higher Reynolds numbers, i.e., up to 10^6. Experimental data on heat transfer coefficients for such high Reynolds numbers are not available, and a check of the correctness of eq. (25) for these high values will only be possible when such measurements are carried out.

Eagle and Ferguson carried out heat transfer measurements for a flow of water in tubes where the temperature differences were very small. The tests were made at a maximum Reynolds number of 3×10^5 and have been extrapolated in 10^6 by the author. The test points are included in Fig. 8. They are situated slightly below the curve based on eq. (25) but confirm otherwise the general trend of the curve.

It should be noted that eq. (25), like eq. (24), is based on Prandtl's original assumption regarding the thickness of the laminar layer (see diagram I of Fig. 4) and will therefore be valid for lower Prandtl numbers (below 10). The new ζ-formula of eq. (23) can, however, also be used in the more complete formulae for heat transfer which are also valid for higher Prandtl numbers.

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Calculation of the Most Economic Current Density

By C. MANZITTI. (From *L'Elettrotecnica*, Vol. 38, No. 10, October 10/25, 1951, pp. 463-465, 1 illustration.)

The question discussed in this article is whether the criteria employed for the determination of the economic density of current in electrical distribution networks are such as to ensure that maximum utilisation of the power generated is obtained at minimum operating cost.

PARTICULARLY in regard to the distribution networks of industrial producers, the adequacy of the criteria for optimum economy is questionable, for the reasons which will now be given.

The rule formulated by Lord Kelvin towards the end of the last century states that an electric line will operate under conditions of maximum economical efficiency if the annual line losses are equal to the burden of the interest and amortisation of the copper used in the line itself.

Objections to this rule were made by a number of authorities. In particular, in 1903 Professor G. Semenza expressed doubts regarding its applicability to electrotechnical problems. Without going into details, he expressed the view that Lord Kelvin was taking only one of the factors of the problem into consideration, since in practice not only the current intensity but also energy must be taken into account. In a paper on this subject,* Prof. Semenza indicated the advantages of graphical methods and added that it would be impossible to establish a single formula despite attempts made by distinguished scientists such as Kapp, Perrino and Forbes to adapt to practice Lord Kelvin's rule, since each problem presented completely different conditions which precluded a general solution.

The purpose of this article is to introduce a factor of some importance which has not been previously taken into consideration in economy criteria, viz., the energy effectively utilised, this being equal to the difference between the energy input and the energy dissipated along the line. This quantity varies according to the section considered and cannot, therefore, be represented by a constant in dealing with this problem. By including this element, the problem is placed on the basis of a transport of energy, and not of current only.

The further loss which is now being considered is related, from the point of view of the industrial producer, to decreased production as a result of the line loss, in addition to the line loss itself.

This fact will be rendered more evident by means of the following reasoning. To fix the ideas, it will be convenient to consider the case of a line with a determined load and a given number of hours of utilisation per annum, consisting of conductors having a section s_e, calculated according to Lord Kelvin's rule.

The line losses, which will be denoted by a, in this case will be equal to the financial burden corresponding to the interest and amortisation for the copper of the line, so that this item may also be denoted by a.

Therefore, we can define S_e as the sum of these two quantities, so that: $S_e = 2a$.

The industrial undertaking will utilise the part of the energy input F remaining after deducting the energy dissipated by line losses, that is, $F - a$.

We shall now consider that the same line operating under similar conditions has conductors with a section equal to ns_e. In this case, the annual line loss is (a/n) and the item corresponding to interests and amortisation for the copper is (an). The sum of these two terms is, therefore,

$$S = (a/n) + an$$

and the energy effectively used will be that part of the energy input F which is nett of the energy dissipated through line losses, that is,

$$F - (a/n).$$

*See: Acts of the Italian Electrotechnical Association, Vol. 7, p. 611.

The economic conditions can now be compared for the two cases considered. By adopting the section s_e, we obtain a difference in the annual expenditure for the two losses previously mentioned, this being equal to

$$S - S_e = an + (a/n) - 2a = (a/n)(n-1)^2$$

Furthermore, a corresponding variation is obtained for the energy effectively utilised R, which can be written as

$$R = [F - (a/n)] - (F - a) = (a/n)(n-1).$$

Now, if we can demonstrate that there exist values of n for which the difference

$$D = R - (S - S_e) = (a/n)(-n^2 + 3n - 2) > 0,$$

we shall be able to conclude that the section s_e, calculated according to Lord Kelvin's rule, is not the most suitable and that, on the contrary it is the section ns_e which should be regarded as such, with the value of n determined so that a maximum value is obtained for the expression

$$D = a(-n^2 + 3n - 2)/n.$$

This expression for D is of the second degree and has two real roots $n_1 = 1$ and $n_2 = 2$, so that $D = 0$ for these two values. The function $D > 0$ assumes a maximum value when $n = \sqrt{2}$, since $\partial D/\partial n = (-n^2 a + 2a)/n^2 = 0$ which gives $n = \sqrt{2}$ as a maximum value. Therefore, it will be more suitable to adopt the value $S_e\sqrt{2}$ for the section instead of S_e.

This has the result of increasing the annual expenditure for the two items representing line loss and interest plus amortisation of copper:

$$S - S_e = a\sqrt{2} + (a/\sqrt{2}) - 2a = (a/2)(3\sqrt{2} - 4) = 0{\cdot}1213a,$$

but it also corresponds to an increased utilisation of energy per annum:

$$a - (a/\sqrt{2}) = (a/2)(2 - \sqrt{2}) = 0{\cdot}2929a$$

and the nett gain per annum is, therefore,

$$(a/2)(2-\sqrt{2}) - (a/2)(3\sqrt{2} - 4) = a(3 - 2\sqrt{2}) = 0{\cdot}1716a,$$

which shows that the energy recuperated will cost a fraction only of the amount on which the contract price of the supplying company is based. The ratio of the two costs is

$$[(a/2)(3\sqrt{2} - 4)]/[(a/2)(2 - \sqrt{2})] = \sqrt{2} - 1 = 0{\cdot}4142.$$

The foregoing explanation demonstrates the possibility of recovering 29·29 per cent of the line loss calculated by Lord Kelvin's rule, for an increase in the annual expenditure equal to 12·13 per cent of the line loss. The recuperated energy is therefore considerable, particularly in periods of shortage of hydraulic power when its value should be computed at four times the normal price, owing to the higher cost of energy produced thermally.

A similar reasoning can be following for the case of an industrial firm producing its own energy. The gain obtainable is the same as previously, but it is achieved by generating a smaller amount of energy for the same quantity of energy utilised.

In a previous note on this same subject, reference

was made to the case of a convertor plant for the electrolysis of zinc, in which alternating current was converted into direct current of a determined power, which could not be increased. In view of the impossibility in this case of varying the loss by reduced production through withdrawal of an additional amount of energy equivalent to the line loss, it was shown that it was possible to determine the economic density of current by means of the following formula:—

$$I/s = (1/40)\sqrt{0{\cdot}7a\,\alpha/[100N(p + \alpha'\pi/100k)]}$$

where I = current in amperes generated by the converters (total current); s = section of conductors connected to bus bars; $\sqrt{0{\cdot}7}/40$ = constant for copper, a function of its resistivity and specific gravity; $a/100$ = fraction representing interest and amortisation for copper, used for the supply bars; α = price of copper per metric ton; N = number of hours of utilisation per annum of the power generated by the converters; p = price of the watt-hour at the terminals of the cells; π = price of zinc per unit of weight; k = consumption in watt-hours corresponding to the production of one unit weight of zinc; and $\alpha'/100$ = fraction of the price of the unit weight of zinc corresponding to the loss as a result of decreased production.

Considering further the formula given above, it is easy to see that $\alpha'/100k$ is nothing else than the loss due to reduced production corresponding to a line loss of one watt-hour.

Moreover, as Lord Kelvin's formula can also be represented by the expression

(*Concluded on page* 61)

U. S. A.

Modern Leaf Spring Design for Automobiles

By F. T. ROWLAND. (From *Automotive Industries*, Vol. 105, No. 12, December 15, 1951, pp. 38-40, 4 illustrations.)

FORMULAE used in leaf spring design, including correction factors, although providing a basis for preliminary work, do not reveal the stresses which may develop under various operating conditions. Good leaf spring design relies to a large extent on a knowledge of these stresses, which can be determined by the use of strain gauges.

Fatigue failure usually occurs in an area of localised stresses or stress concentration. The most efficient spring design would have uniform stress throughout the length and width of each leaf. This cannot entirely be accomplished, since the spring must have a means of attachment to the frame and to the axle seat, in which areas the stress is lower than the stress in the balance of the spring. Also, under bending, the cross-section of the spring leaf is distorted, and both edge tension surfaces are therefore further removed from the neutral axis than the middle tension surface. Since the magnitude of the stress at any point in the cross-section is proportional to its distance from the neutral axis, the edge tension surfaces will have a higher stress than the middle tension surface.

If the spring steel section has concavity as allowed in the steel mill rolling tolerance, the stresses at the edge tension surface would be higher under load than the stress at the middle tension surface. The differential between the stresses at the edge and middle of the tension surface apparently remain the same until about 40,000 psi, which point may vary depending on width and gauge of the section. Beyond this point of bending, due to distortion of the section, the stress at the edge tension surfaces increases at a greater rate than the stress at the middle tension surface.

Because of these conditions, the surface or skin stress varies from eye to eye in the main leaf, as shown in Fig. 1. It also varies from one tension edge of the cross-section to the other tension edge. The leaf spring for long fatigue life must be designed so that there should be no points of stress which extend abruptly above the nominal stress for any loading. A stress pattern must follow a smooth line as shown in Fig. 1. Within the spring seat, the stress pattern will depend on the design of the seat and whether or not insulating material is used.

With high-powered engines, the rear leaf spring must be carefully designed to absorb the driving and braking torque. This condition, together with the demand for softer-riding automobiles, has advanced to the point where exacting stress analysis of leaf springs under dynamic loading is required before the design can be approved for use.

Soft-riding springs require a low rate. The softer springs are less able to resist torque wind-up and often are over-stressed under severe power application. Repeated over-stressing will cause early fatigue failure or settling of the spring.

Leaf springs absorb much of any sudden application of power from the engine or brakes, and thus ease the shock or jolt which is transmitted to the passengers. When this happens, the leaf springs wind up into a flat " S " shape and, in so doing, store energy for the moment, this energy being released as the spring returns to the normal position.

In order to reduce the possibility of overstressing due to torque application, longer springs are employed. For the same rate, the longer spring will require a greater moment of inertia which, in turn, gives greater strength or resistance to wind-up. In the longer springs, for equivalent stress, the leaves can be of heavier gauge, thus increasing the mechanical strength of the main leaf eyes. These advantages can be obtained with little or no increase in the weight of the spring.

Strain gauges may be used to verify calculated stresses and to establish the extent of stresses added due to torque. The strain gauges are cemented to the

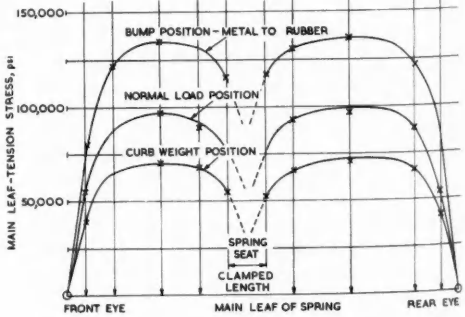

Fig. 1. **Stress pattern of a main leaf spring for three loadings with the automobile stationary. The normal load includes curb weight plus the payload.**

tension surface of the main leaf. Their position will be governed by the design of the spring and may be based on peak stress areas. For the main leaf analysis, 10 gauges along the middle of the leaf will give a good coverage with possibly two additional gauges near the edge, which will establish the relation between the edge and middle tension surface stresses.

Readings of the strain gauges can be taken for any loading of the automobile. Fig. 1 shows a stress pattern of the main leaf for three loadings with the automobile stationary.

Strain gauges have their greatest value for dynamic stress analysis with the automobile under actual driving conditions. The gauges are connected to a recorder, which may be assembled and operated on the rear seat of the automobile. Acceleration, deceleration, road conditions, right or left turns and speeds may be spotted and appropriately marked as the recording is produced.

The recordings will give the extent of the stresses under the various driving and road conditions. Stress recordings can be made over a test course and comparative spring designs evaluated. Fig. 2 shows a stress pattern of the main leaf under static plus dynamic loadings.

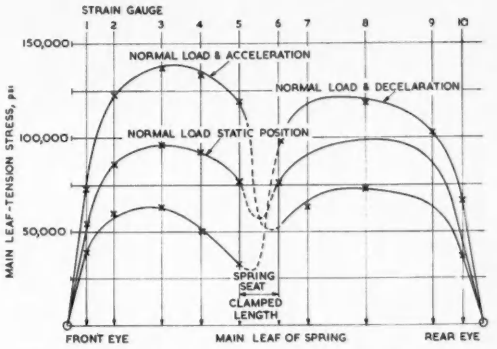

Fig. 2. **Stress pattern of a main leaf spring under static plus dynamic loadings.**

From the information and data developed on comparative spring designs, a decision can be made with a high degree of confidence as to the results which will be obtained in service.

SWEDEN

Static Frequency Transformers for Small Electric Motors

By F. DAHLGREN and A. K. LITTKE-PERSSON. (From *Acta Polytechnica*, 82 (1951), Electrical Engineering Series, Vol. 3, No. 6, 24 pages, 22 illustrations.)

IN many applications, there is a need for high-speed low-horsepower motors. Generally, either a series-commutator motor operating at the mains frequency or a single or three-phase synchronous motor operating a a higher frequency (such as 200 cps) is used. However, the series-commutator motor with its commutator and brush gear requires periodic maintenance, and its speed characteristic is not always suitable, particularly when the operating schedule includes mechanical no-load periods. The high-frequency asynchronous motor, on the other hand, requires a converter unit to generate a high-frequency voltage.

It was therefore considered advisable to make a systematic investigation of small asynchronous motors operated by means of static frequency transformers. Although the overall efficiency cannot be high, and a large quantity of materials is required per unit of power, the advantages of these sets lie in their operating characteristics, particularly with regard to starting and mechanical overload capacity.

Static frequency transformation is obtained by a combination of non-linear circuit elements, which can be divided into the following groups: (a) dry cell valves, (b) ferro-magnetic elements such as saturated transformers or reactors and transductors (pre-saturated reactors), and (c) varistors and thermistors. In view of the very low efficiencies obtained with elements of the third group, these were not included in the investigation. A theoretical investigation was made of the various possible arrangements of elements of groups (a) and (b). This was followed by tests, first with a load consisting of a fixed resistance and inductance and then with a load constituted by a single-phase squirrel-cage motor.

FREQUENCY-DOUBLER CIRCUITS

Frequency-doubling can be obtained by two different methods, viz., (1) by means of valves performing the change-over as a function of the polarity of the applied voltage, and (2) by means of transductors operating according to the direction of the mainly reactive input current.

The valves give the usual voltage curves obtained, for instance, with full-wave rectification. The transductor voltage curves are of the cosine type with vertical discontinuities. The total voltage variation is $U\sqrt{2}$ for the valve circuits, and $2U\sqrt{2}$ for the transductors. A harmonic analysis of these curves shows that the voltage of the first harmonic of the double-frequency system is 0·484 of the applied voltage in the first case, and 0·848 in the latter case. The transductor circuit is therefore more suitable.

A number of frequency-doubling circuits were considered, with separate magnetising windings and with auto-magnetisation. It was found possible to increase the efficiency considerably by replacing the Graetz bridge by an ordinary full-wave rectifier fed from the terminal of a reactor. Series magnetisation gave a further slight improvement. It is important that the current passing through the rectifier valves should always be close to the rated value, and preferably also that only one valve path be used in series. The efficiency is also dependent on smoothing and on the grade of core plate used for the transformers, so that the best grade obtainable should be employed. A circuit diagram of the final frequency-doubler circuit tested is shown in Fig. 1.

The transductor elements had an iron cross-section of 8 cm² and an effective length of 29 cm. With $N_1 =$ 316 turns on the primary and a variable number of turns N_2 on the secondary side, it was found that with a constant RC load of 6 ohms and 170 μF and a primary voltage of 160-190 volts at 50 cps frequency, the best result was obtained for $N_1/N_2 = 3$ and $L = 0{\cdot}05$ henry. The magnetising current varied from 2·8 to 3·25 amperes, and the active input power P_2 of the load from 60 to 80 watts. The primary power factor was $\cos \varphi_1 = 0{\cdot}55$ (lagging) and the efficiency obtained was

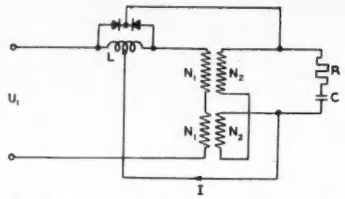

Fig. 1. **Circuit diagram of frequency-doubler.**

38-40 per cent. The value of R was subsequently varied between 15 and 40 ohms, and C was varied between 90 and 170 μF. The highest efficiency obtained reached 45-50 per cent.

The RC-load was then replaced by a single-phase asynchronous motor with a fixed capacitor in the auxiliary winding. The machine was tested as a symmetrical two-phase motor fed in a Scott connection. With a transformer having a voltage ratio of 256/146 volts connected in the circuit for matching, and a series capacitor for obtaining a reactive load and cutting off the magnetising current, the most favourable results were achieved for a primary voltage of 180 volts, using

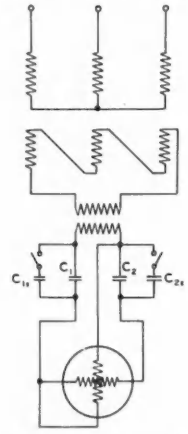

Fig. 2. **Circuit diagram of frequency-tripler.**

a series capacitor of 37 μF. The highest efficiency was obtained when the resistance of the motor and capacitor in the auxiliary winding was equal to 15 ohms, as seen from the primary side of the intermediate transformer. The magnetising current I_m was then 2·2-2·6 amperes, with P_1 = 250 watts and η = 43 per cent. In the tests with series magnetisation and constant primary voltage, it was found that the output power of the transductors increases at an approximately linear rate with the magnetising current, whereas η generally reaches a final value when I_m is greater than 2·2 amperes. This explains why series magnetisation does not give a higher efficiency than parallel magnetisation if the magnetising current increases beyond a critical value. The efficiency of the motor was about 40 per cent. It may be expected that with transductors of improved design, it should be possible to increase the efficiency of the frequency-doubler circuit to 50-60 per cent.

FREQUENCY-TRIPLER CIRCUITS

The frequency-tripler circuits to be considered comprise a three-phase supply to a transformer core with five limbs or to three separate single-phase transformers. The primary winding is star-connected with an insulated neutral, whereas the secondary has an open delta connection. The fundamental component of the magnetic flux in the iron core is dependent on the voltage between lines. The 3rd, 9th, etc., harmonics are not present in the primary current, but occur in the magnetic flux at a high saturation, irrespective of the primary voltage, and they induce corresponding e.m.f. harmonics in the secondary circuit. Therefore, the load is based on the tripled supply frequency as a fundamental component. In most cases, it is an advantage to have a series capacitor of moderate size, in order to obtain a combined ohmic and capacitive load. The load can consist of a single-phase motor. Alternatively, a symmetrical two-phase motor can also be operated by means of a circuit comprising two independent frequency transformers differing by 90 degrees in the secondary phase at the secondary frequency.

Fig. 2 shows the two-phase motor fed in Scott connection which was used for tests. For matching the frequency transformer and the motor, a transformer with a voltage ratio of 110/26 was used. The capacitors had the following values: C_1 = 6 μF, C_{1s} = 56 μF, C_2 = 175 μF, and C_{2s} = 130 μF. The subscript s indicates that the capacitor was connected during the starting period only. Typical test results with the motor supplied at a tripled frequency of 150 cps are given in the following table.

U_1	Active power			Transformer efficiency			
	Transformer		Motor output	S/P ratio*		Actual efficiency	
	Prim.	Sec.		Motor	RC-circuit	Motor	RC-circuit
280	1200	960	419	20	—	80	—
280	1200	930	—	—	17	—	78
270	896	720	336	20	—	80	—
270	850	630	—	—	17	—	74
270	415	260	86	8	—	63	—
270	440	290	—	—	9	—	64
270	400	250	64	9	—	63	—
270	400	260	—	—	8	—	65
255	590	450	200	18	—	76	—
255	550	400	—	—	16	—	73

*S/P = ratio of secondary active power output to primary apparent power input, neglecting losses and leakage reactances.

The transformer data are as follows: Cross-section of limbs = 0·00174 m²; space between limbs: height = 150 mm, width = 58 mm; effective length of magnetic circuit = 330 mm per limb; primary winding 220 turns on each of the three inner limbs, wire diameter 2 mm; secondary winding 500 turns on each of the outer limbs, wire diameter 1·2 mm.

In regard to the starting characteristics, it should be borne in mind that since the frequency transformer is considerably affected by the magnitude and phase angle of the load, and the decrease in the secondary voltage U_3 is particularly marked when the load impedance is lower than the optimum value, difficulties are encountered in starting the asynchronous motor. The impedance of the motor at starting was about one-third of the operating impedance, and U_3 at starting was scarcely half the operating value. To obtain a favourable current distribution in the motor and an appropriate phase angle for the transformer, it is necessary to increase both C_1 and C_2 during the starting period. Consequently, the starting torque of the motor is low when the motor is fed from the frequency transformer, but, on the other hand, the primary side is insensitive to starting, so that a direct start can always be permitted. Another difficulty is due to a certain amount of residual torque corresponding to the fundamental component of the voltage, which is counteractive at certain motor speeds. Owing to the asymmetry of the transformer, it was not possible to eliminate this component entirely†.

†A simplified theoretical treatment of the frequency tripler circuit with performance curves is given in the original source.

New Materials, Processes and Equipment

NEW TENSILE TESTER ACCESSORIES

Several new accessories have been added to this Tensile Tester manufactured by Instron Engineering Corporation, Quincy 71, Mass., greatly increasing its versatility.

Outstanding among the features of this precision electronic testing instrument is its wide full scale range, extending from 2 grams to 5000 pounds. In addition, a wide selection of jaw speeds is provided, ranging from 0·02 to 20 ipm. The combination of a versatile system for changing the load sensitivity and push-button switching for operating the moving crosshead, make available fast routine measurements as well as more complex testing techniques fundamental to a research and development programme.

The electronic load-weighing system is based on the electric strain gauge principle. Characteristic of this instrument is its high speed of response and virtually zero motion of the upper jaw. The motion of the lower jaw is controlled by a positional servo-system ensuring a constant rate of elongation in either direction and providing almost instant starting, stopping or reversing.

An auxiliary and easily removable crosshead is attached to the frame below the moving crosshead. Compression type load cells have a rigidly attached load-sensitive table. The sample is compressed between this table and the moving crosshead.

The same flexibility of control for operating the motion of the crosshead in tensile measurements is now possible for compression testing. Load deformation, cyclic, and relaxation data can be obtained with the same accuracy and high-speed response. By the use of simple jigs and fixtures, torsion and flexural measurements may be carried out to evaluate more completely the mechanical properties of the test specimen.

An integrator has been developed by means of which, the area under the load-deformation curve, equivalent to the energy either absorbed or returned by the test sample, is immediately available from counter readings.

NEW POLYESTER RESIN

Development of a new polyester laminating resin which will withstand temperatures as high as 500° F, was announced by Naugatuck Chemical division, United States Rubber Company. The new resin is expected to find its most significant use as a structural material in the manufacture of high-speed aircraft and guided missiles. It is now being tested for this type of application at the Wright Air Development Center in Wright Field, Ohio.

The new resin has physical and handling properties comparable to other types of polyesters now available with the added benefit of high strength at high temperatures. Laminates made with the resin and glass fabric retain good flexural strength (40,000-47,000 psi) when exposed for as long as 200 hours at a temperature of 300° F. Good flexural strength (over 30,000 psi) is also obtained when exposed for as long as 24 hours at 500° F. The heat resisting properties of the resin are made possible by the use of a new chemical, triallyl cyanurate.

Initially the resin will be available for military applications. However, Naugatuck Chemical is boosting production capacity in order to supply resin fabricators who require heat resistance for applications in the electrical, electronic, radio and television industries. It is being marketed as Vibrin X-1047.

HIGH-ACCURACY VOLTAGE TRANSFORMER

A special transportable super-accuracy voltage transformer has recently been supplied by Foster Transformers & Switchgear Ltd. (a " Lancashire Dynamo " company) to the National Physical Laboratory. It has ranges from 10 to 40 kV and, at the rated burden of 1 VA, the ratio error is within 0·03 per cent over 40-60 cps and from 30-110 per cent of the rated voltage. The phase angle under the same conditions is within one minute.

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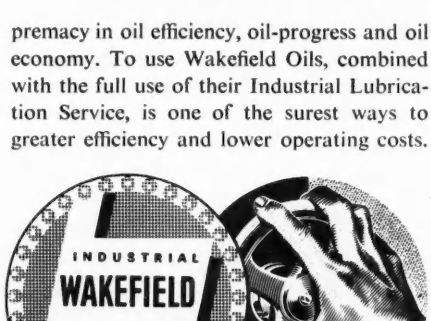

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CLASSIFIED ABSTRACTS

Numerous important abstracts which, for lack of space, cannot be published in our main editorial columns, are regularly included in condensed form in this section. Subscribers may obtain photostat copies of all original articles at cost.

HEAT TREATMENT

The Function of Energisers in Pack Carburising

By H. HULTGREN. From *Jernkontorets Annaler, Sweden*, Vol. 135, No. 11, November 1951, pp. 575-604, 3 illustrations, 16 tables.)

AFTER studying the various ways in which the energisers K_2CO_3, Na_2CO_3, and $BaCO_3$ may be placed in the carburiser box with crushed charcoal and the objects to be carburised, two methods are found to give the same results as mixing charcoal and energiser :

(1) K_2CO_3 or Na_2CO_3 applied separately, e.g., as a layer on top of the charcoal. These two energisers, on being heated to the carburising temperature, rapidly spread out, forming a uniform mixture with the charcoal.

(2) $BaCO_3$, applied as a thin coating on the steel surface, preferably with the aid of a binding agent, such as lacquer. The coated specimens are packed in unmixed charcoal which has been washed thoroughly with dilute hydrochloric acid. As $BaCO_3$ has very small spreading power, it remains where it is applied, exercising its energising function near the steel surface ; apparently, the presence of an energiser is not required beyond a certain small distance from this surface. In carrying out the carburising process with $BaCO_3$-coated steel objects, the presence of a small amount of K_2CO_3 in the charcoal or in the coating may interfere with the carburising reaction in some unknown manner, thus leading to poor results.

When a carburiser is used repeatedly, it is necessary to mix a minimum quantity of fresh crushed charcoal, about 10 per cent of the total charge, with the spent carburiser each time it is re-used. A small amount of energiser should be added occasionally to compensate for the small evaporation and handling losses.

The decline in carburising power of a powerful carburiser used repeatedly without any additions, noticeable after two or three carburising cycles, is probably caused by a rapid loss in active surface area of the charcoal particles, and certainly not by loss of energiser.

Energisers in carburising compounds act as catalysers for the reaction $CO_2 + C \rightleftarrows 2\,CO$. In this way a high CO concentration is maintained near the steel surface and carburisation is accelerated.

IRON AND STEEL

Ageing Phenomena in Iron and Steel after Rapid Cooling

By J. D. FAST. (From *Philips Technical Review, Holland*, Vol. 13, No. 6, December, 1951, pp. 165-171, 8 illustrations.)

AFTER quenching of iron and steel, a slow change often takes place in the mechanical properties of the material. This quench ageing is to be ascribed to the presence of certain constituents in the metal. The hardness of α-iron containing small quantities of carbon, nitrogen or oxygen separately has been determined after various heat treatments. Also, the influence of manganese on the ageing of α-iron with carbon or nitrogen has been studied. This investigation has been confined to a study of the changes in hardness taking place after quenching from the α-phase ; thus, from a temperature lying below the eutectoid temperatures of the materials, the technically still more important case of quenching from the γ-phase has been left out of consideration. It appears that oxygen causes virtually no quench ageing ; this element is, in fact, almost insoluble in α-iron. Carbon and nitrogen are to some extent soluble in α-iron, and both cause considerable quench ageing. This is to ascribed to precipitation of carbon and nitrogen in the form of carbide and nitride, from their supersaturated solution in the iron formed by rapid cooling. Manganese appears to be capable of suppressing the quench ageing of iron containing nitrogen but not that of iron containing carbon. Considering the comparatively great affinity between manganese and nitrogen, an explanation is sought by assuming that in the crystal lattice the nitrogen atoms will preferably occur in the vicinity of manganese atoms, where they are bound much more strongly than when surrounded exclusively by iron atoms, so that the formation of iron nitride, and thus the precipitation, take place at a perceptible rate only at high temperatures.

ROLLING MILL PRACTICE

New Rolling Method Eliminates Surface Defects

By N. C. RENDLEMAN. (From *The Iron Age, U.S.A.*, Vol. 168, No. 20, November 15, 1951, pp. 122-124, 8 illustrations.)

DIAMOND and square rolling is now being used with considerable success to roll seams, spongy surfaces and other defects out of semi-finished products on the secondary mills. The first experiments were made in 1947 and a number of mills are now being installed with this type of reduction for the production of rods and bars.

The bite of the rolls in the diamond pass is such that no roughening of the roll surfaces is needed ; the corners of the original billet are not displaced or moved into the centre of the sides as in box passing. In the diamond passes, the sides of the square are transversely stretched. This action appears to pull open the seams, thus decreasing their depth and eventually causing them to disappear entirely. When using this method, scarfing and chipping to remove defects are not necessary, even in difficult Bessemer grades of steel with sulphur ranges of 0·160-0·320 per cent.

Two ingots of Bessemer grade steel with seamy, spongy and cracked surfaces were used for tests. The blooms were rolled in a diamond and square mill into $1\frac{3}{4}$-in. billets without conditioning of any kind. The seams on the blooms, although still present, were stretched out so that they had practically no depth. The $1\frac{3}{4}$ in. billets, rolled from the same seamy blooms, showed practically no sponge or cracks, and little evidence of any seams after their final reduction. The entire product was rolled into $\frac{13}{16}$-in. rounds and then cold-drawn into $\frac{1}{2}$ in. rounds without any conditioning. Magnetic inspection of these rounds showed only 48 lb. of the 483 lb rejected with tops deeper than 0·021 in. On the basis of commercial standards of inspection, the total rejects were as follows :—

	Laps	Slivers	Total
Ingot I	2·64%	2·32%	4·96%
Ingot II	—	4·0%	4·0%

THE CHEMICAL INDUSTRY

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SHEET METAL

Parts Flattened by Annealing Under Weights

By H. CHASE. (From *The Iron Age, U.S.A.*, Vol. 186, No. 18, November 1, 1951, pp. 134-136, 4 illustrations.)

WHERE close flatness limits are specified, they often necessitate expensive hand straightening. Even this imposes stresses which may vary under certain conditions, resulting in subsequent waste or trouble. If flatness could be held within fairly close limits at the end of an annealing cycle calculated to remove all stresses, subsequent departures from flatness with resultant troubles and expense would be minimised.

Annealing under clamping pressure was tried but did not give satisfactory results. However, if parts were laid or stacked between flat faces in fixtures of proper design under the moderate, floating yet constant, pressure of a weight, good results could be attained. In fact, by this means flatness is often held within 0·003 in. In general, pressure applied is below 1 psi, but is higher for an alloy steel part than for one made of low carbon steel of the same thickness. It is essential that the fixtures remain flat and that the parts be placed with care and not stacked too deep.

Some tests may be required to obtain optimum results. If fixtures warp, as they may after repeated use, they must be trued by machining. Parts annealed in a warped fixture may have to be processed again.

WELDING

Nitrogen Arc Welding of Copper

By A. N. SHASHKOV and Z. S. KHROMOV. (From *Avtogennoye Delo, Russia*, No. 9, 1951, pp. 4-7, 6 illustrations.)

INVESTIGATIONS into the argon arc welding of copper have shown that this method of welding enables improved concentration of heat, which ensures a stable welding process, a relatively high welding speed, and a good seam formation. However, the protection against oxidation is adequate only when small quantities of a reducing flux are added to the molten pool. These can be applied in the form of a thin coating on the filler rod. Thus, seams having 85-90 per cent of the tensile strength and elongation of the virgin metal are obtained.

The high cost of argon instigated a search for a cheaper substitute. Chemically, nitrogen is a practically inert gas in relation to copper. Tests were carried out with a nitrogen-shielded welding arc between a tungsten electrode and the welded metal. Butt welds between copper sheets ranging in thickness from 1 to 3 mm were used for evaluation. Filler rods of M1 copper with diameters of 1·6, 2 and 3 mm respectively were used. Standard equipment for argon arc welding was employed.

For an identical short-circuit setting of the welding generator, a d.c. arc in nitrogen has a 30 per cent higher voltage, a 10 per cent lower current, and 15 per cent higher power than it has in argon. Assuming a correctly shaped electrode, a stable arc is obtained with a straight polarity circuit. When polarity is reversed, fast burning of the electrode tip is observed. Hence, a.c. welding is not practicable, but with straight polarity in d.c. welding, the wear on the electrode is much smaller than in the argon arc process.

Nitrogen has a higher heat conductivity and a higher specific heat than argon, and hence carries away more heat from the arc into the molten pool. Nitrogen has 1·2 times the exit velocity of argon at equal pressure. It mixes more easily with air. Tests indicate the use of 1·5 times the gas pressure of argon when identical electrodes and gas nozzles are used. In all, a higher arc power can be utilised, and with uncoated filler rod, the welding speed can be increased by 25-30 per cent. However, without flux, the nitrogen arc process is as unreliable as the argon arc process, and results in wide scatter of the mechanical properties, though the appearance of the seam is good.

A large number of fluxes was tested. The best flux was a mixture containing phosphorus and copper, but its introduction into the weld is difficult. In practice, ferro-phosphorus proved adequate as a substitute. In view of the small percentage required, the introduction of iron as an impurity is not considered detrimental. A mixed flux consisting of charcoal, ferro-phosphorus, aluminium powder, ferrosilicon and ferromanganese gave best results. When a reducing flux is used, nitrogen and argon arc-welded joints have nearly identical mechanical properties, but the nitrogen arc seam is broader in appearance.

CALCULATION OF THE MOST ECONOMICAL CURRENT DENSITY

(*Concluded from page* 56)

$$I/s = (1/40)\sqrt{0{\cdot}7a\,\alpha/(100Np)}$$

it can be concluded that, in order to take account of the loss due to reduced production corresponding to the line loss, Lord Kelvin's formula will be applicable if it is modified by replacing the effective cost p of the watt-hour by an expression for this same cost increased by the loss due to reduced production per watt-hour of line loss. This applies when the loss due to reduced production cannot be avoided, as in the case of an electrolytic plant, by the withdrawal of an extra amount of energy equal to the line loss.

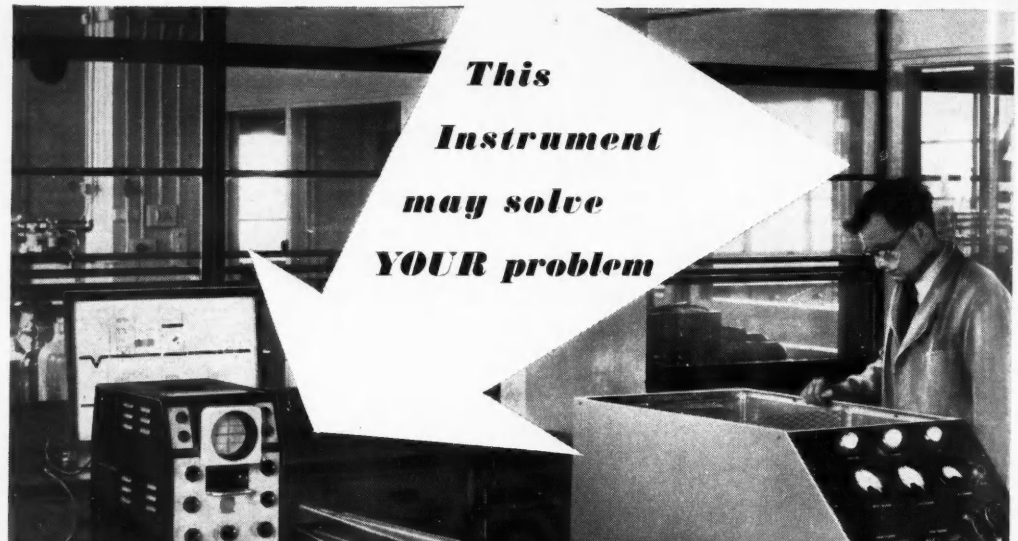
In the case, however, of a normal industrial undertaking, in which the loss due to reduced production can be avoided by withdrawing an additional quantity of energy, the term $(\alpha' \pi/100k)$ can be replaced directly by the cost of the watt-hour p.

Therefore, in such a case Lord Kelvin's formula can also be applied for the determination of the economic density of current, if the effective cost p of the watt-hour is replaced by $2p$, which is twice the original value. This is fully in agreement with the results indicated above, according to which the true economic current density is equal to the value calculated by Lord Kelvin's formula, multiplied by the factor $1/\sqrt{2}$.

Thus, it may be concluded that the usual criteria do not ensure that maximum operating economy is obtained in electrical distribution networks.

In order to maintain over an extended period of time an optimum efficiency for an electrical distribution line with a determined load and a given annual hourly utilisation, it would be necessary that a plus or minus percentage variation of the loss due to the interest and amortisation, should also correspond to an equal plus or minus variation of the other losses.

It is not conceivable that such a condition will prevail in the near future. On the contrary, it may be expected that a decrease in the cost of copper will occur when the present crisis is overcome, and an increase in the cost of the energy as a result of the readjustment called for by the electrical companies owing to the changed conditions of the market and the cost of wages. Furthermore, it is not possible to exclude the repeated occurrence of periods of hydroelectric power shortage during the winter and the corresponding burden of the increased price for electricity produced by thermal means. Therefore, the density of the current should fall off to values appreciably lower than those now prevailing, particularly since one should bear in mind the deteriorated situation in which Italy is now struggling, Owing to the devaluation of the lira, as a result of the economic situation Italy is compelled to sustain a line loss which under otherwise similar conditions is three times that obtaining in the United States. This has the effect, in Italy, of making the ratio between the cost of the unit of copper and the cost of the kilowatt-hour three times greater than the corresponding figure quoted in America.



Here in the Central Laboratories of the West Midlands Gas Board, the Cossor Double Beam Oscillograph is being used to observe voltage and current transients in a high voltage discharge through gas in an electro de-tarring process. Photograph by courtesy of "GAS TIMES".

Designed by electronic engineers who are intimately concerned with the problems which technicians have to face, the Cossor Double Beam Oscillograph will enable the waveforms of two related though *independent* phenomena to be examined *simultaneously*. Many industries, ranging from nylon spinning to the manufacture of jet engines, are today using this versatile instrument for an inexhaustable variety of functions in research and production, and it often provides the answer to a long-standing problem. Please consult our technical advisory staff who are always at your service to help with *your* particular problem.

Here are some brief details: The two traces are presented on a flat screen of 90 mm. diameter and the calibrated controls of the instrument enable the measurement of the input voltage to each amplifier as well as the Time interval between various significant portions of the Oscillogram. Permanent records of these traces for subsequent analysis may conveniently be made by attachment of the Cossor Model 1428 Camera.

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NEWS OF THE MONTH

PERSONAL

Mr. G. R. Ball has been elected a director of the International Nickel Co. of Canada Ltd.

Mr. G. P. Belsham has been appointed joint managing director of Brookhirst Switchgear Ltd., Chester. **Mr. K. N. Swash, B.Sc., A.M.I.E.E.,** has been appointed to the Board of the company.

Mr. W. A. Boggia has been appointed to the newly created position of supervisor of production and procurement at Remington Rand Ltd., Commonwealth House, New Oxford Street, London, W.C.1.

Rear-Admiral G. F. Burghard, D.S.O., has been lent to the Ministry of Supply as deputy controller of electronics.

Mr. R. H. Cruickshank has been appointed Buyer for G. & J. Weir Ltd., Cathcart, Glasgow, S.4.

Mr. J. V. Daniel has been appointed a director of Crompton Parkinson Ltd., Crompton House, Aldwych, London, W.C.2.

Mr. E. C. Davies has been appointed manager of the heavy plant section of the engineering sales department of The General Electric Co. Ltd., Witton, Birmingham, in succession to **Mr. H. Nielsen. Mr. G. F. J. Morgan** has been appointed assistant to Mr. Davies.

Mr. A. N. Dodson has been appointed regional controller of the Ministry of Fuel and Power, Midland Region, Birmingham.

Mr. B. H. Dyson, who until recently was deputy general works manager, has been appointed general works manager for the four main factories of Hoover Ltd. In addition to holding this responsibility for the whole organisation, he is a director of Hoover (Washing Machines) Ltd.

Mr. W. A. Gallon, B.Sc., M.I.E.E., M.Inst.F., chief engineer of the South Eastern Electricity Board, has been appointed controller of the Merseyside and North Wales generation division of the British Electricity Authority. He succeeds **Mr. A. R. Cooper, M.I.E.E., M.Inst.F.,** now controller of the North Western division. **Mr. H. B. Campbell, A.M.I.Mech.E., M.Inst.F.,** superintendent of the Cliff Quay power station, Ipswich, has been appointed generation engineer (construction) of the Eastern division.

Mr. R. F. W. Guy, B.Sc., M.I.Mech.E., M.Inst.F., has joined the consulting staff of Powell Duffryn Technical Services Ltd. as a senior engineer attached to the firm's Manchester office in Lord's Chambers, 26 Corporation Street, Manchester 4.

Mr. E. Le Q. Herbert, B.Sc.(Edin.), F.R.I.C., has been appointed general manager of the Shell Refining & Marketing Co. Ltd.

Mr. E. E. Ingleton has been appointed works manager of the Yorkshire Engine Co. Ltd., Sheffield.

Mr. H. F. H. Jones, M.B.E., J.P., M.Inst.Gas.E., A.M.I.C.E., has been appointed deputy chairman of the Gas Council, 1 Grosvenor Place, London, S.W.1.

Mr. J. T. Lidbury has been elected a director of Hawker Aircraft Ltd. and Hawker Aircraft (Blackpool) Ltd.

Mr. J. H. Lomas, general manager of Sheepbridge Stokes Ltd. and British Van der Horst Ltd., **Mr. H. Gunner,** general manager of Sheepbridge Equipment Ltd., **Mr. Z. Z. Kosarski,** general manager of Sheepbridge Steel Castings Ltd., **Mr. J. W. Lennox,** general manager of Sintered Products Ltd., **Mr. A. E. S. Gourlay,** general manager of Clews Petersen Ltd., **Mr. R. F. Elliott,** general manager of Light Production Ltd., and **Mr. E. A. Macdonell,** general manager of Sheepbridge Stokes Engine Liners Ltd., have been appointed directors of their respective companies.

Sir Andrew McCance, D.Sc., M.I.Mech.E., F.R.S., has accepted the position of Chairman of the Mechanical Engineering Research Board of the Department of Scientific and Industrial Research in succession to the retiring Chairman, **Sir Henry Guy, C.B.E., D.Sc., M.I.C.E., M.I.Mech.E., F.R.S.**

Mr. George J. Muntean has been appointed assistant superintendent of The Timken Roller Bearing Co., Canton 6, Ohio, U.S.A.

Mr. John Rae, has retired from the chairmanship of McKechnie Brothers Ltd., but retains his directorship. **Mr. J. D. McKechnie** has been appointed chairman and retains his position of managing director of the company.

Viscount Runciman, O.B.E., A.F.C., President of the Institution of Naval Architects, has been elected President of the Chamber of Shipping of the United Kingdom, 3-6 Bury Court, St. Mary Axe, London, E.C.3. **Mr. D. F. Anderson, M.A.(Oxon),** has been elected vice-president.

Mr. Matthew Seaman, M.I.Mech.E., has been appointed a director of British Oxygen Engineering Ltd. (a subsidiary of the British Oxygen Co. Ltd.) Angel Road, Upper Edmonton, N.18.

Dr. Esmond Wassell Smith, Ph.D., B.Sc., M.I.E.E., one of the leading authorities on world submarine telegraph and telephone cables, has been appointed to the boards of The Telegraph Construction & Maintenance Co. Ltd. and its subsidiary, The Selborne Plantation Co. Ltd.

Mr. R. Walker, secretary, and **Mr. B. P. Hutton,** sales manager, have been appointed directors of Marryat & Scott Ltd., Wellington Road South, Hounslow, Middlesex.

Mr. A. E. Watkins, M.Inst.F., and **Mr. S. W. Spurr, M.Inst.F.,** have been elected directors of Spencer-Bonecourt Ltd., 14 Fetter Lane, London, E.C.4. **Mr. Watkins** has also been appointed managing director. **Captain (E) W. Gregson, R.N.R., M.Sc.(Eng.), M.I.C.E., M.I.Mech.E., M.Inst.N.A.,** continues as chairman and technical director.

Sir John Woods, G.C.B., M.V.O., has been elected a director of the English Electric Co. Ltd., and of Marconi's Wireless Telegraph Co. Ltd.

OBITUARY

The General Electric Company Ltd., announces with deep regret the death on Sunday, 27th January, of Mr. Frank Lonsdale, a director of the Company and late general manager of the Company's Fraser and Chalmers Engineering Works, Erith.

Mr. Lonsdale, who was aged 68 years, joined Fraser and Chalmers in 1909 and, after a period in the turbine section, was appointed chief of the drawing office, and subsequently manager of the turbine contracts department. In 1918 when the firm was taken over by The General Electric Co., Ltd., Mr. Lonsdale became sales manager, and in 1938 was appointed general manager. Mr. Lonsdale had been a director of The General Electric Co. Ltd., since 1946.

BUSINESS NOTES

Aircraft Research Association Ltd. A group of companies in the Aircraft Industry has registered the Aircraft Research Association Ltd., the purpose of which is to design, build and operate what will be the largest trans-sonic/supersonic wind tunnel in this country for testing scale models of aircraft.

The tunnel will require approximately 35,000 horse-power. Negotiations are in hand with the Ministry of Supply and other authorities, on the siting of the wind tunnel, the supply of power and other services.

CURTA....

A NEW CALCULATING MACHINE DESIGNED ESPECIALLY TO SUIT the needs of the ENGINEERING INDUSTRY

Modern precision engineering often calls for greater accuracy than is possible by the slide-rule. The CURTA, with a capacity of 8×6 with 11 figures in the product register, is exactly what the Engineering Designer requires.

CURTAS are now proving their worth with Drawing Offices in more than 100 of the foremost engineering concerns in this country, as well as in many famous organizations on the Continent.

You are invited to visit Stand No. 31 at the Business Efficiency Exhibition (Birmingham)

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The turbine oil with the 3 protections

CALTEX REGAL OILS (R & O) are manufactured by specialised processes to keep lubricating systems clean, bearing temperatures normal, and governing action instantly responsive.

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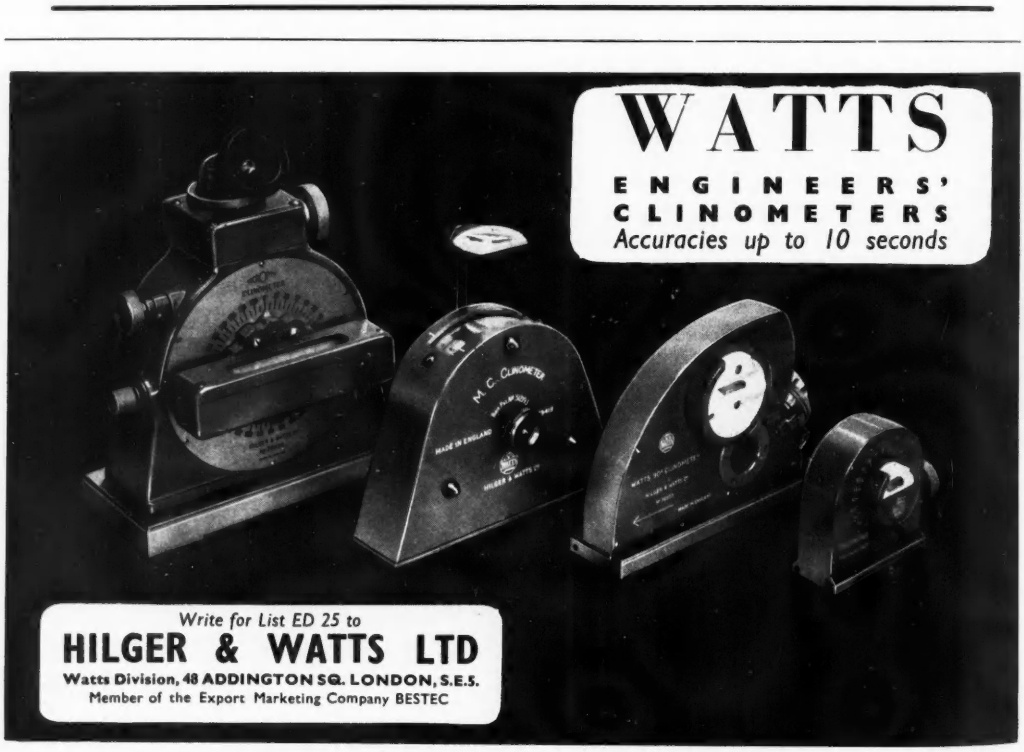

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The project was initiated by the Technical Board of the Society of British Aircraft Constructors, which reported that the progress of British aircraft design and development demanded provision of a large wind tunnel capable of operating at speeds just below and higher than the speed of sound, and available at all times for use by the Industry.

The companies concerned are: Blackburn & General Aircraft Ltd., Boulton Paul Aircraft Ltd., The Bristol Aeroplane Co., Ltd., The Fairey Aviation Co., Ltd., Folland Aircraft Ltd., Handley Page Ltd., The Hawker Siddeley Group Ltd., Rolls-Royce Ltd., Saunders-Roe Ltd., Vickers-Armstrongs Ltd., Westland Aircraft Ltd.

The registered offices of the new company are at 32, Savile Row, London, W.1.

British Driver-Harris Co. Ltd., of Manchester, announce that their Fine Wire Section has been considerably extended and re-equipped for the production of standard Nickel Chrome, Copper Nickel and Pure Nickel wire, in sizes down to 50s SWG. New machines of the most modern pattern have been installed, and present delivery is good.

Weymouth Company wins big American contract. A heartening story of British engineering in competition with American firms for an important contract in the United States comes from Bristol's Instrument Company of Weymouth. This is particularly remarkable because the U.S. competitors included the parent Company of Bristol's.

The contract was for the instrumentation of a large coke oven plant at St. Louis, Missouri, which is being built by Simon Carves Ltd., who again secured the contract in the face of keenest American competition.

The instrument control panel is twenty feet long and seven feet high, and incorporates instruments which measure, record and automatically control pressures, temperatures, and rate of flow. A number of features of instrumentation and control are used which have never before been applied to coke oven plants. Some of the instruments are designs adopted from the parent Company, and exporting them back to America—so to speak—creates an interesting and perhaps significant precedent.

This instrument panel, which would normally represent one year's delivery for the Weymouth factory, was completed in six months.

Lever Brothers & Unilever Limited announce that at an Extraordinary General Meeting to be held on February 27 at Unilever House, a special resolution will be submitted to change the Company's name to **Unilever Limited.** A similar resolution to change the name of the Dutch parent Company to Unilever N.V., will be submitted to an Extraordinary General Meeting of the shareholders of that Company.

The Institute of Gas Technology, 17 West 34th Street, Chicago, has resumed publication of its Research Bulletin Series, suspended since 1948. The fruits of the intervening three years of basic and applied research are being issued in a series of sixteen bulletins.

Two of them, "Sulfur Poisoning of Nickel Catalysts," by C. H. Riesz, H. A. Dirksen, and W. J. Kirkpatrick, and "Expansion Behavior of Coal During Carbonization," by O. P. Brysch and W. E. Ball, have already been published. A third, "Pilot Plant Catalytic Gasification of Hydrocarbons," by C. H. Riesz, P. C. Lurie, C. L. Tsaros and E. S. Pettyjohn, is in the hands of the printers. Others are to appear as rapidly as careful editing and printing permit.

Pirelli-General Cable Works Ltd., have received an order to manufacture and install an insulated cable for operation at a higher voltage than any other industrial cable in the world. This will be made at their Eastleigh Works, Hampshire.

The order is for 0·4 sq. in. single conductor oil-filled cable and sealing ends to form a 750 yard termination for an overhead line in Canada, operating at 301,000 V, three-phase, with centre point earthed.

The cable will be graded dielectric construction with the lead sheath metal-tape-reinforced and served overall with anti-corrosive finish. The carrying capacity of each cable is 530 amps and the three-phase circuit will carry 275 M.V.A. The cable and all accessories have been designed by Pirelli-General in conjunction with Mr. Emanueli, of Pirelli, Milan.

This cable will be employed in connection with a new melting plant to be built for the Aluminium Company of Canada on the site of a deserted Indian village—Kitmat, some 400 miles from Vancouver, British Columbia. A new town of 50,000 inhabitants will eventually be built on the site and the aluminium plant will produce 1,000,000,000 lbs of refined metal per year.

Fenter Ltd., Birmingham, have been appointed world agents (excluding Australia and New Zealand) for the complete range of Denbigh milling and drilling machines, metal saws and presses.

Brown, O'Sullivan and Partners, consulting engineers, have moved to 123 Victoria Street, London, S.W.1. Tel. No.: TATE GALLERY 0266.

The new address of the Aircraft Section of **Alan Muntz & Co. Ltd.** is Langley Aerodrome, Bucks. Tel. No.: Slough 23861.

C. M. Hill & Co. (Engineers) Ltd., 44/5 Tower Hill, London, E.C.3, have been appointed agents for S. A. Trains de Roues du Centre, manufacturers of light railway wagons.

Conveyancer Fork Trucks Ltd., Warrington, and Rubery, Owen & Co., Ltd., Wrexham, are staging an Owen Organisation Materials Handling Convention on Friday, June 27, at their Whitegate factory, Wrexham. Products of about 40 factories of the Owen Organisation will be on show.

INTERNATIONAL MACHINE TOOL EXHIBITION, LONDON, 1952

The International Machine Tool Exhibition, organised by The Machine Tool Trades Association, Victoria House, Southampton Row, London, W.C.1, will be held at the Olympia, London from September 17 to October 4.

Exhibits will comprise Machine Tools, Engineers' Small Tools, Gauges, Measuring Equipment, Testing Equipment, Presses and Hammers, Heat Treatment Plant and Woodworking Machinery.

Apart from Great Britain, exhibits will be shown from leading machine tool manufacturing countries, including the United States, France, Germany, Holland, Italy and the Scandinavian countries.

POWER STATION FOR MECCA

The Holy City of Mecca is to be supplied with electricity through the co-operation of leading British firms, the main contracts having been placed with the Brush Aboe Group. As non-Moslems are not allowed within sight of the city many interesting problems will have to be overcome.

The power station will be sited about four miles from Mecca, just outside the Moslem confines, where engineers can work without restriction. British Insulated Callender's Construction Company Ltd. are responsible for the planning and supply of the transmission, distribution and service connections.

Over 26 miles of B.I.C.C. cables will be used to supply power for fans, refrigeration, air-conditioning plant and lighting for all public buildings, including palaces.

From the power station to the city will run a 7,000-yard 11 kV double circuit overhead transmission line, with a further 15,000 yards of 11 kV cable in the underground ring main. The project will be completed by 20,000 yards of overhead line and 4,000 yards of underground LV service cable.

VERSATILE NEW COMPRESSOR

A new fully developed and tested compressor unit of sturdily simple design is now in quantity production at The Hymatic Engineering Company Ltd., Redditch, Worcs. Designated the 100 SAS, the compressor was built to an exacting specification for belt drive or direct coupling at speeds of up to 2000 r.p.m. It is, therefore, eminently suitable for fitting to tractors, cultivators, motor vehicles, machine tools and so on, either as initial equipment or as an optional extra. With a displacement of up to 7·5 cu. ft. per min. at pressures of 80-100 p.s.i., the 100 SAS has the performance necessary for spray painting, lime washing, insecticide spraying, pneumatic greasing, tyre inflation, pneumatic braking, the operation of air tools, controls, jigs and fixtures and a hundred and one other compressed air applications.

The unit consists basically of a single stage, air cooled, 6·56 cu. in. capacity compressor, cylinder bore 1·97 in. and stroke

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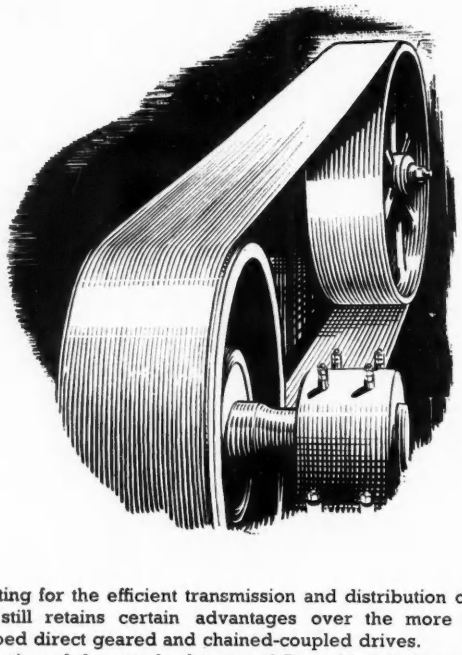
DUNLOP BELTING—
TO MEET ALL CONDITIONS



Flat belting for the efficient transmission and distribution of motive power still retains certain advantages over the more recently developed direct geared and chained-coupled drives.
Construction of the standard types of flat belting includes plies of closely woven, pre-stretched cotton duck, which is manufactured in Dunlop textile factories, impregnated with rubber compounded to meet varying service conditions of speed, load, temperatures, moisture, and dust-laden atmospheres. Modern production methods allied to the skill acquired by years of craftsmanship make Dunlop Flat Transmission Belting second to none.

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1G GM5A

2·16 in., pedestal mounted on the totally enclosed cast iron crankcase. The drive pulley, 8⅝ ins. in diameter, serves a dual purpose in operation, being so designed as to pass a stream of cooling air over the unit.

The cylinder has a cast iron liner inside its finned aluminium barrel, thus combining the hard wearing properties of the former and the high heat dispersion of the latter. The light alloy piston is fitted with one scraper and two compression rings. The connecting rod is an "I" section steel forging with roller bearings in the big end and a phosphor bronze bush in the small end. Smooth running and long life are further ensured by the copper brazed overhung crankshaft with its integral balance weight and the wrapped bush type main bearing. Valves are of the low lift plate disc type with large air passages to give low air speeds and greater efficiency. The construction is such that the suction and delivery valve assembly can be easily removed as one unit.

Splash type lubrication is employed to provide an adequate oil supply to the cylinder, piston, big end and main bearings. The maximum level of the oil in the sump is controlled by the level of the filler plug and over-oiling is, therefore, impossible. A drain is provided in the lower edge of the crankcase close to one of the three slotted feet which form the base fixing. Oil consumption is 2·25 cc./hr. or 1 pt. for every 250 hours of running time. The oil sump holds 1¼ pts. An efficient oil wetted air intake filter is supplied unless otherwise stated.

Weighing 36 lb. complete, this new and versatile Hymatic compressor absorbs only 1·34 H.P. at 2000 r.p.m. when delivering its full capacity at 80 p.s.i. and 1 H.P. at 1500 r.p.m.

RADAR FOR SMALL AIRPORTS

A simple one-man radar aid for guiding aircraft in to land has been designed for small airfields. The prototype is now in use at Southend Airport. It is similar to the Ground Controlled Approach (GCA) but much cheaper: it will cost about £3-3,500, about one-twentieth the cost of GCA.

LATEST INDUSTRIAL LITERATURE

1. New Slide Rule for Belting Calculators. Calculating rule designed for finding the belt required for any particular drive by simply setting the slide so that the pulley diameter coincides with the pulley revolutions per minute. It is then possible to read off at a glance belt speed in feet per minute and the width of the belt required to transmit any given horse power. The calculator may also be used in a variety of ways when designing belt drives. A table provides the minimum recommended pulley diameters for all thicknesses and types of belting.

2. Stopped! For What? New illustrated 16-page brochure just issued stresses the importance of production control systems. Explains how systems and equipment can ease production difficulties and keep machines running. Production is shown as a complex network of operations, each interdependent, and each able to be controlled effectively by one of the several systems illustrated in the brochure.

3. Concentric Centring. Illustrated 16-page brochure describes fully automatic single head and double head centring machines, including a machine specially designed for centring and milling large forgings. The brochure also contains specifications of the latest 6½ in. centring and spot facing machine which centres both ends of the workpiece simultaneously.

4. Roller Levelling Machines. Profoundly illustrated 12-page brochure presents descriptions, specifications and photographic illustrations of a range of sheet and strip levelling machine and heavy supported roll levellers. Description of a 53-ton levelling machine of the latest design, embodying many refinements not previously incorporated in this type of machine, is included. In addition, photographic illustrations of round bar straightening machines, section straightening and forming machines, rolling mills and tube straightening machines are presented. The brochure is edited in three languages, English, French and Spanish.

5. Improved Non-metallic Conduit and Fittings. Illustrated brochure presenting details of a new non-metallic conduit which is, it is claimed, the ideal plastic material for electrical conduit and fittings.

6. Oil Retaining Bearings. 12-page technical brochure and 28-page complementary catalogue deals with oil retaining bearings for smooth and efficient running of all types of machines. The carefully designed brochure offers much useful data and other information on the design and application of sintered metal bearings.

7. All-Electric Airless Spraygun. Illustrated folder describes all-electric sprayguns of the latest design. The completely self-contained unit weighs only 3 lb. and does not depend upon air in any form. It is supplied to suit voltages from 110 upwards, a.c. or d.c. Advantages include easy cleaning, rapid coverage, one simple adjustment for volume of spray, almost complete absence from mist and bounce and easy servicing.

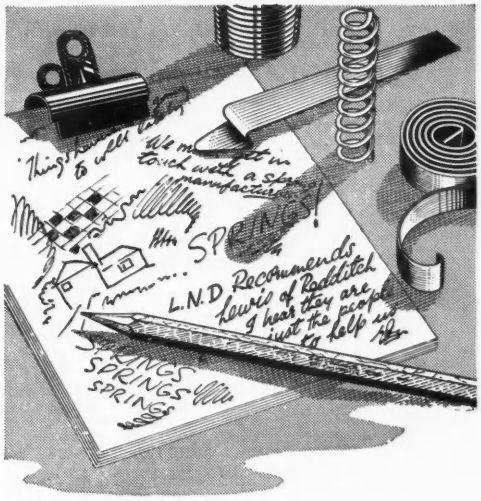
8. Invertors for Industry. In large steelworks and similar situations, the regeneration during rapid braking of large d.c. motors provides a difficult problem due to the voltage rise which occurs on the d.c. network. A loading resistor rapidly switched into circuit usually by electronic means (such as the firing of an ignitron) has been used for limiting such rises. The use of an invertor, however, permits the excess d.c. power to be returned to the a.c. mains and provides far closer control of voltage rise during regeneration periods, apart from reducing energy consumption. Reprint of an article on the rectifier-invertor includes brief details of the history of the theory of operation, construction and performance.

9. Radius Attachment. Illustrated brochure describes radius attachment for work on lathes, surface grinders, cylindrical grinders, vertical and horizontal milling machines, etc. It may also be used as an indexing attachment for jig boring. The equipment includes universal head, diamond holder and indexing finger. Centres and blocks can be supplied if required. A selection of components which can be processed by the radius attachment is shown in photographic illustration.

10. Vertical Gear Generating Machines. Illustrated brochure describes the general principle and design of the latest model of vertical gear generator operating on the continuous rotary generating principle, but employing a single cutter spindle reciprocating in a vertical plane. Details of the machine are fully illustrated. Specifications are included.

11. Pure Oxide Ware. Leaflet describes and illustrates a range of pure oxide tubes, crucibles and furnace shapes used for high temperature melting and heat treatment processes, especially where freedom from contamination is essential. Suitable for use as protecting sheaths for thermocouples where conditions are severe due to the presence of highly reactive slags or furnace gases. Can be used with rare metal thermocouples up to the limit of their range of operation as no attack of the rare metal by silica will be encountered.

12. Dust Control. Illustrated brochure describes a combined fan and dust collector in a single unit which performs both functions with a higher efficiency than the standard exhauster, cyclone or filter which it replaces. It simplifies dust control, eliminates long runs of ducting and saves valuable floor space. Four types for a variety of applications are described and illustrated, their features combining confinement of dust at source, constant collecting efficiency, manual or automatic handling of collected material without creating a secondary dust nuisance and no collecting elements to clog, clean or maintain.



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HERMETICALLY SEALED POTENTIOMETERS, MODEL R5

Wire wound—ohmic value range covers from 5 to 5,000 ohms. Rating 5 watts. Dimensions $1\frac{7}{8}$" diameter x $1\frac{1}{4}$". Weight 3 oz. (without knob). Withstands tropical damp and humidity and arctic cold—salt, dust and chemical laden atmospheres. Complies with inter-service standard specification RCL/131.

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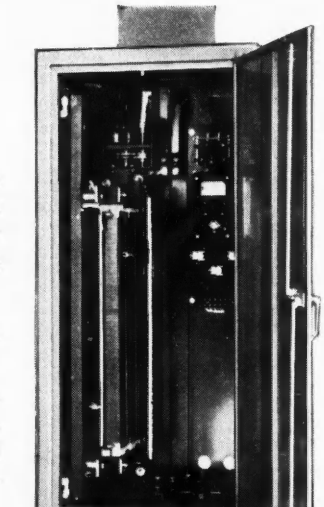
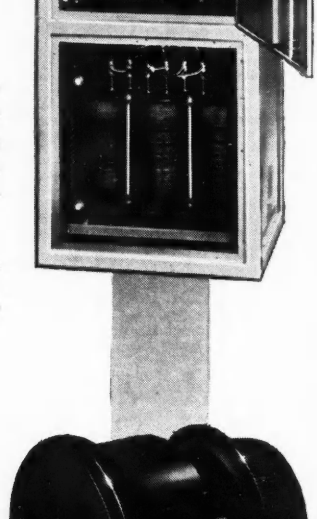
A wide range of vitreous enamelled resistors from 6 watt to 180 watt on 16 standard tube sizes with more than 20 alternative mountings, terminations and fittings. Fully tropical. Exclusive Bercohm craze free, non-porous, vitreous enamel gives stress-free winding and eliminates hot spots. Fully approved by R.C.S.C.

Other BERCO products include fixed and variable wire wound resistances and potentiometers, "Regavolt" infinitely variable auto-transformers, rotary rheostat potentiometers, stud switches, control knobs, etc., for all laboratory and industrial purposes.





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BOOKS

Fractional H.-P. Electric Motors. By F. G. Spreadbury. Publishers: Sir Isaac Pitman & Sons Ltd., Parker Street, Kingsway, London, W.C.2. 342 pp., 227 illustrations. Price: 35/-.

There is little published information dealing in book form with the principles, design, control and performance of fractional h.-p. motors. It is, therefore, a pleasure as well as of value to read and to include this book in the technical library. The importance of such a thorough and comprehensive book on the subject is enhanced by the increasing application of these motors in industry and in other spheres.

The Welding of Non-Ferrous Metals. By E. G. West. With a foreword by Leslie Aitchison. Publishers: Chapman & Hall Ltd., 37 Essex Street, London, W.C.2. 553 pp., 273 illustrations. Price: 55/-.

This book is devoted to the many and complex problems which must be faced and the techniques which must be developed in connection with welding the increasing variety of non-ferrous metals and alloys used in engineering work.

The author quotes nearly 400 references for those who wish to delve deeper into the many facets of the subject. Designers, works engineers and metallurgists on the one hand, and welding engineers, operators and instructors on the other, for whom this book has been written, will gain increased knowledge and derive much benefit from this book, which can be considered amongst the best written on the subject.

All-Purpose Diesels. A practical work for diesel engineers, manufacturers, and users of all types of high-speed oil engines. By J. Malcolm Robson. Publishers: Sir Isaac Pitman & Sons Ltd., Parker Street, Kingsway, London, W.C.2. 316 pp., 232 illustrations. Price: 50/-.

Fundamentals of Lightning Protection (Einiges über die Grundzüge der Blitzschutztechnik). By V. Fritsch. Publishers: Franz Deuticke, Vienna, 1951. 100 pp., 86 illustrations. Price: $6.24.

This is both a practical and a theoretical book, with charts, circuit diagrams, tables and formulae. It first describes methods for the measurement of the electric field and indicates the influences of season, temperature, pressure, etc. The next sections deal with the measurement of the surge current and its magnetisation effects. Diagrams of storm distribution, discharge frequency etc. are given. Numerical data are presented for the electrical properties of the sub-soil and its solid and liquid constituents. These are followed by a theory of equivalent circuits. The measurement of the ohmic propagation resistance by various methods is described in detail. The final sections include diagrams of lightning arrester installations for farms, silos, explosive dumps, houses, churches and large buildings, as well as an outline of specifications and rules for maintenance.

Lancashire and Cornish Boilers of Riveted Construction. (B.S. 537 : 1951)

Horizontal Multitubular Boilers of Riveted Construction. (B.S. 609 : 1951)

Vertical Cross Tube Boilers of Riveted Construction. (B.S. 665 : 1951)

Vertical Multitubular Boilers of Riveted Construction. (B.S. 761 : 1951)

Loco-Type Multitubular Boilers of Riveted Construction. (B.S. 931 : 1951)

The original editions of these standards have been extensively revised. The terms "maximum permissible working pressure" and "safety valve blow-off pressure" in the Factories Act 1937/1948, have been deleted and replaced by the term "design pressure."

These standards deal with materials, construction, workmanship, scantlings, inspection and testing of the boilers. The formulae in the sections dealing with scantlings give in all cases the minimum scantlings and apply to boilers constructed throughout under competent supervision.

Typical methods of attachment of stand-pipes and pads secured by welding alone are given in an appendix to each of these standards. Price 6/- each.

Extra-low Voltage D.C. Cyclic Switches for Aircraft. B.S. G.136 : 1951 covers the requirements for extra-low voltage D.C. electrically-operated switches, with contacts operated in a cyclic manner. The specification does not include switches used in radio, electronic or telephonic apparatus. The switch contacts may be designed for use on any of the recognized aircraft supply systems. Price 1/-.

Calcium Carbide (Graded Sizes). B.S. 642 : 1951 covers eight sizes of grading. Test requirements for dust, gas yield and impurities are stated and methods of sampling and testing are specified. Conditions of sale and purchase as established by the British Acetylene Association are also given in Appendix B.

The main alterations in the revised standard are to be found in Appendix A, in which new apparatus and improved methods for the determination of gas yield and impurities have been included. The specification section now stipulates that the carbide shall yield gas containing not less than 99 per cent by volume of acetylene, instead of 96 per cent as in the original standard. Limits for arsenic and nitrogen compounds have been added to those for sulphur and phosphorus. Appendix B remains unaltered, except that the amounts of gas yield below which claims and rights of refusal are allowed have been slightly increased for all sizes below 15 mm. to accord with the tolerance of minus 5 per cent specified in Clause 5.

During the preparation of the original standard, the Committee had the advantages of the views and co-operation of the Dominion interests concerned, as well as the close collaboration of the associated European manufacturers of carbide. This close co-operation has been continued during the present revision. Price 4/-.

Dimensions of Conduit Dies. B.S. 1813 : 1951 relates to conduit dies, guides and stocks of the type used by electrical contractors. Simplified ranges have been standardized with a view to securing the greatest practicable measure of interchangeability. The principal dimensions for three sizes of dies with corresponding guides and stocks are provided, and recommended screwing ranges of B.S. Conduit threads for each size of die are given. Price 2/-.

Dimensions of Toroidal Sealing Rings. B.S. 1806 : 1951 prepared at the request of the Ministry of Defence, applies to the dimensions of toroidal sealing rings, sometimes referred to as "O" seals, which are used as packings for reciprocating mechanisms and as gaskets for static purposes for light and medium general engineering applications. These sealing rings are not intended for use at pressures in excess of 1500 lb./sq. in.

For use as packings on reciprocating mechanisms 73 sizes are specified in the range of $\frac{1}{8}$ in. to 8 in. and for static purposes 96 sizes in the range of $\frac{1}{8}$ in. to $15\frac{1}{2}$ in. This latter range includes the 73 sizes intended for reciprocating mechanisms.

Recommendations are also included for the recesses into which these rings are fitted, but the standard emphasises the necessity for close collaboration between the designer and the manufacturer of sealing rings to ensure satisfactory operation and service. Price 2/6.

Marking System for Grinding Wheels. B.S. 1814 : 1952 establishes a uniform system of marking to indicate the relative hardness and grit size of grinding wheels. It is based on the agreed practice of British grinding wheel manufacturers, who have adopted this sytem to secure uniformity in the various symbols for the different characteristics of the wheels. The sequence in which the symbols are used gives a general indication of the hardness and grit sizes of any one wheel as compared with another but it will be appreciated that the widely differing conditions under which grinding wheels are used make it impracticable to state that wheels of similar marking made by different manufacturers will necessarily give the same grinding action.

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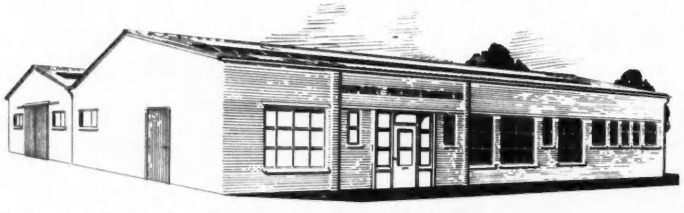

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